The 14th Nordic Laser Materials Processing Conference

NOLAMP 14

Edited by Alexander Kaplan and Hans Engström
14th NOLAMP Conference

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August 26th – 28th 2013

Gothenburg
Sweden

Edited by:
Alexander Kaplan
Hans Engström
Luleå University of Technology
Preface

Laser is a key element in obtaining a sustainable economy in Europe. Innovative laser processes will play a significant role in future green manufacturing as they provide a precise, well-controlled and highly effective energy deposition to the workpiece. Future challenges are to increase the spectrum of laser manufacturing technologies in all sectors where the laser can offer innovative product solutions, higher product quality, less environmental impact, higher productivity and in turn cost benefits.

The NOLAMP conferences address all aspects of laser materials processing from fundamental science to industrial applications. The first NOLAMP conference took place in Oslo in 1987 in a period of intense development of the laser processes and their industrial applications. The NOLAMP has continued on a biannual cycle ever since, strengthening the Nordic laser community by encouraging knowledge transfer and networking. Now, 26 years later, in the forthcoming 14th NOLAMP, the research and development of processes like laser welding, laser hybrid welding, laser cutting and laser surface treatment is still very important, as well as the development of industrial applications and equipment for laser material processing.

This time the NOLAMP Conference will be held in Gothenburg, Sweden. In this highly industrialized region of Sweden and with the close distance to Denmark, Norway and Finland, we hope to attract a strong participation from industry and the scientific community.

On behalf of the Nordic laser community and Luleå University of Technology, it is our great pleasure to invite you to the 14th NOLAMP conference.

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Information can be also found at the conference web site:
www.ltu.se/nolamp14
**14th NOLAMP Conference**
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Gothenburg, August 26-28, 2013

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STATUS AND FUTURE TRENDS IN LASER APPLICATIONS

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Abstract

Laser material processing is already an inherent part of today’s industrial fabrication worldwide with constant growing fields of new applications. The laser enables the possibilities for a competitive, modern manufacturing technology which gives the users many advantages like higher speeds, improved product quality, less heat input, better materials utilization and very high system availability. Successful companies use laser materials processing as a natural technology in their manufacturing.

The laser is established in all common production techniques from primary shaping (laser ablation), forming (laser bending), separating (laser cutting and drilling), joining (laser welding, laser brazing) to coating (laser metal deposition) and change of material properties (laser hardening, laser annealing).

If you think about lasers in modern mass production automotive industry comes in your mind immediately. As a matter of fact, automotive industry is most likely the sector with the highest use of high power lasers for material processing, which is predominantly laser cutting and laser joining.

Once forming and hardening in the jöt stamping process is complete, 3-D laser systems are responsible for trimming the outside edges of the parts and for adding openings and holes. Unlike in mechanical trimming, the hardness of the material is of no concern to the laser. Especially in 3-D processing, laser tools have substantial advantages over other technologies. They are more flexible and more cost effective. In laser welding either fixed optics or intelligent scanner optics position the laser beam on the work piece by means of rapid mirror movements. And with welding on the fly” scanner mirror axis movements are superimposed on robot movements, so the focal point moves from one welding seam to another on the car body part in just a few milliseconds. That way, three or four laser seams can typically be welded every second — reducing overall car body welding times by a factor of five. Thanks to the increased productivity of the laser technique, production lines need less welding cells, which reduces the required floor space in body manufacturing. In addition the laser technique is considerably less energy-intensive than conventional spot welding, so it brings down CO2 emissions attributed to cars even before they reach the road.

The automobile has a big future, but a lot of things have to change: better fuel economy, lower weight, fewer emissions and new drive concepts. Thus new applications arise like cutting carbon fiber reinforced plastics (CFRP), welding of battery housings or fuel cells and components of electric motors. The laser has to proof to be a suitable production tool to meet the challenges of these applications on the long run.
In the meantime other industry sectors don’t need to hide behind automotive if we talk about laser application in serial production. Especially in the field of consumer electronics the product cycles are relative short, product changes are incisive, volumes are extremely high and time to market is of essence. This provides a perfect platform for the laser as a flexible tool and creates constantly new applications such as cutting of glass and processing of non metallic materials.

**Keywords:** cutting, welding, automotive
INFLUENCE OF LOW ENERGY LASER WELDING ON SOLIDIFICATION AND MICROSTRUCTURE OF AUSTENITIC STAINLESS STEEL WELDS

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Abstract

Primary austenitic solidification is related to increased hot cracking susceptibility in welding of austenitic stainless steels. It is also recognised that high cooling rates and rapid solidification conditions, like those achieved in laser beam welding (LBW), increase the stability of austenite versus ferrite as the primary solidification phase. Knowledge about the solidification mode under LBW conditions is therefore of utmost importance. A series of austenitic stainless steel alloys were prepared using an electric arc furnace and cooled at a rate of 10 °C/s. The overall alloying composition was kept constant at [Cr\textsubscript{eq}+Ni\textsubscript{eq}] = 40 wt% while changing the Cr\textsubscript{eq}/Ni\textsubscript{eq} ratio from 1.52 to 1.84. These alloys were then laser welded using a continuous wave ytterbium fibre laser at two different energy input levels. Cooling rates were experimentally determined to be in the range of 10\textsuperscript{3} °C/s to 10\textsuperscript{4} °C/s and the values were confirmed by computational modelling. The compositional border between primary austenitic and primary ferritic solidification was found to shift to higher Cr\textsubscript{eq}/Ni\textsubscript{eq} values at higher cooling rates. However, all the alloys showed coexistence of regions of primary austenitic and primary ferritic solidification for both laser settings although austenite tended to more abundant at higher cooling rates. Austenite content and refinement of microstructure is discussed in terms of effects of cooling rate on solidification behaviour and solid state transformations.

Keywords

Laser welding, ytterbium fibre laser, austenitic stainless steel, ferrite, solidification modes, Ferritic-Austenitic solidification mode, Austenitic-Ferritic solidification mode, cooling rate, dendrite arm spacing.
1- Introduction

Hot cracking can be experienced by austenitic stainless steels during welding, in particular if the weld solidifies exclusively as austenite. The causes have been extensively studied and it has been demonstrated that impurities such as sulphur and phosphorus tend to segregate to the liquid phase and form low melting point eutectics, in particular during primary solidification as austenite [1-8]. The nature and distribution of these eutectics along the grain boundaries at the last stages of solidification, together with the solidification shrinkage and the restraining forces are considered to be the main causes of solidification cracking. Solidification cracking is more likely for fully austenitic A and austenitic-ferritic AF solidification. In both solidification modes, austenite is the primary solidification phase, but in AF some ferrite is formed in the austenite boundaries because of the eutectic reaction experienced by the last-solidifying interdendritic liquid. Weld metals with primary ferritic solidification modes: ferritic-austenitic FA and fully ferritic F are less prone to solidification cracking because the solubility of impurities in ferrite phase is higher.

Low energy input welding processes, such as laser beam welding (LBW) and electron beam welding (EBW) can result in high cooling rates. It is well-known that high cooling rates can promote austenite as primary solidification phase due to the dendrite tip undercooling phenomenon [9-13]. Therefore, an austenitic alloy that under arc welding conditions solidifies as FA and does not present a risk of hot cracking, when laser welded can shift to primary austenitic solidification and can become prone to hot cracking. Consequently, the study of the transition between primary austenitic and primary ferritic solidification modes AF-FA is of utmost importance under low energy laser welding conditions.

Traditionally, the solidification mode has been related to the parameter (chromium equivalent)/ (nickel equivalent) ratio (Cr_{eq}/Ni_{eq}). However, it was recently found [14] that the transition AF-FA depends also on the overall alloy content. For arc welding conditions and an overall alloy content of [Cr_{eq}+Ni_{eq}] = 30 wt%, the critical Cr_{eq}/Ni_{eq} ratio was between 1.38 and 1.55, while in case of [Cr_{eq}+Ni_{eq}] = 40 wt% the critical Cr_{eq}/Ni_{eq} ratio was between 1.28 and 1.32 (Hammar and Svensson’s equivalents: Cr_{eq}= Cr + 1.37Mo and Ni_{eq}= Ni + 0.31Mn + 22C + 14.2N). Some studies have been conducted to investigate the effect of low energy welding processes on the transition between solidification modes but none of them considered the effect of the overall alloy content (Table 1).

Due to the experimental difficulties of measuring cooling rates in low energy laser welds, traditionally cooling rates have been estimated by different correlations [19-22]. These relate the thermal variables of the process with the resulting dendrite morphology, normally with the Dendrite Arm Spacing (DAS). However, in this study an attempt was made to measure cooling rates experimentally. In addition, an advanced computational method developed by one of the authors has been applied to calculate cooling rates. Differently from previous work, the transition between solidification modes was evaluated at a fixed overall alloy content and related to cooling rate. Moreover, an ytterbium fibre laser was used rather than Nd-YAG and CO₂ lasers as in earlier studies.
### Table 1 - Summary of previous research related to AF-FA transitions for low energy input welding.

<table>
<thead>
<tr>
<th>Researcher</th>
<th>Process</th>
<th>Cooling rate (^{°}C/s)</th>
<th>Equivalents</th>
<th>Transition AF-FA</th>
<th>Alloy or ([Cr_{eq}+Ni_{eq}])</th>
</tr>
</thead>
<tbody>
<tr>
<td>Katayama et al. [15]</td>
<td>Nd-YAG</td>
<td>(2\times10^{-5})</td>
<td>Schaeffer</td>
<td>At approx. 8%-vol. ferrite. Schaeffer diagram modified including transitions for laser.</td>
<td>AISI 3XX alloys</td>
</tr>
<tr>
<td>Elmer et al. [16]</td>
<td>CO₂ laser</td>
<td>(3\times10^{-5} - 5\times10^{-7}) (\text{DAS})</td>
<td>Pure Cr, Ni</td>
<td>Alloy 58.6%Fe-25.5%Cr-15.8%Ni presents coexistence of solidification modes when cast-button and when EB remelted.</td>
<td>7 different alloys with Cr+Ni between 40.7-41.7% and Cr/Ni between 1.16 and 2.21</td>
</tr>
<tr>
<td>Lippold [9]</td>
<td>EB</td>
<td>(4.7\times10^{-5} - 7.5\times10^{-8}) (\text{DAS})</td>
<td>Suutila, Delong, WRC-1992</td>
<td>Transition between (Cr_{eq}/Ni_{eq} = 1.55 - 1.65) (WRC-1992)</td>
<td>316L, 304L, 321Mo. Approx. around 30% (Cr_{eq}+Ni_{eq})</td>
</tr>
<tr>
<td>Fukumoto et al. [17]</td>
<td>Nas-YAG</td>
<td>Not measured nor calculated.</td>
<td>Pure elements Cr, Ni</td>
<td>From their Fig. 14, transitions are approx. found between Cr/Ni 1.55 to 1.60</td>
<td>Fe-18%Cr(10-14)%Ni (Cr+Ni) =30% approx.</td>
</tr>
<tr>
<td>Brooks et al. [18]</td>
<td>CW CO₂</td>
<td>Semi-analytical model: temperature gradient 690+/−150 K/mm at growth rate of 1 mm/s</td>
<td>Pure elements Cr, Ni</td>
<td>From their Fig. 14, transitions are approx. found between Cr/Ni 1.55 to 1.60</td>
<td>Fe-18%Cr(10-14)%Ni (Cr+Ni) =30% approx.</td>
</tr>
</tbody>
</table>

2- Experimental work

2.1. - Alloy selection

The overall alloy content was fixed at \([Cr_{eq}+Ni_{eq}] = 40\%\). When selecting alloy compositions the results from previous researchers’ findings in terms of AF-FA transitions were considered (Table1). Therefore, 6 samples with a fixed 40 \% \([Cr_{eq}+Ni_{eq}]\) overall alloy content but with \(Cr_{eq}/Ni_{eq}\) ratios from 1.52 to 1.84 were prepared. With this wide range of ratios, the transition ratio predicted by Katayama [15] was covered and included also lower and higher ratios. Each sample was prepared by melting combinations of GTAW wires (AWS SFA5.9 ER310, ER312 and AWS SFA5.18 ER70S-6) with a total batch weight of 50 g using an electric arc furnace according to ASTM E1306-07 in a pure argon atmosphere. The final button-shaped alloys were cut and their chemical composition analysed (Table 2).

### Table 2 - Chemical composition of the alloys (% wt)

<table>
<thead>
<tr>
<th>Alloy</th>
<th>C</th>
<th>Mn</th>
<th>Si</th>
<th>S</th>
<th>P</th>
<th>Cr</th>
<th>Ni</th>
<th>N</th>
<th>Mo</th>
<th>(Cr_{eq}/Ni_{eq}) (\text{Schaeffer})</th>
<th>(Cr_{eq}/Ni_{eq}) (\text{H&amp;S})</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>0.0862</td>
<td>1.658</td>
<td>0.4850</td>
<td>0.0054</td>
<td>0.0149</td>
<td>23.81</td>
<td>12.60</td>
<td>0.055</td>
<td>0.0875</td>
<td>1.54</td>
<td>1.52</td>
</tr>
<tr>
<td>2</td>
<td>0.0874</td>
<td>1.652</td>
<td>0.4662</td>
<td>0.0039</td>
<td>0.0179</td>
<td>24.15</td>
<td>12.30</td>
<td>0.045</td>
<td>0.1294</td>
<td>1.59</td>
<td>1.58</td>
</tr>
<tr>
<td>3</td>
<td>0.1233</td>
<td>1.726</td>
<td>0.4705</td>
<td>0.0043</td>
<td>0.0157</td>
<td>24.94</td>
<td>11.22</td>
<td>0.084</td>
<td>0.0952</td>
<td>1.63</td>
<td>1.60</td>
</tr>
<tr>
<td>4</td>
<td>0.0945</td>
<td>1.608</td>
<td>0.4129</td>
<td>0.0054</td>
<td>0.0191</td>
<td>24.26</td>
<td>11.68</td>
<td>0.047</td>
<td>0.1318</td>
<td>1.67</td>
<td>1.66</td>
</tr>
<tr>
<td>5</td>
<td>0.1111</td>
<td>1.695</td>
<td>0.4533</td>
<td>0.0049</td>
<td>0.0172</td>
<td>25.73</td>
<td>10.70</td>
<td>0.087</td>
<td>0.0962</td>
<td>1.78</td>
<td>1.74</td>
</tr>
<tr>
<td>6</td>
<td>0.0836</td>
<td>1.703</td>
<td>0.4711</td>
<td>0.0056</td>
<td>0.0180</td>
<td>25.83</td>
<td>10.64</td>
<td>0.076</td>
<td>0.0969</td>
<td>1.90</td>
<td>1.84</td>
</tr>
</tbody>
</table>

LECO combustion technique for N analysis. For the rest of elements OES (Optical Emission Spectroscopy) analysis.
2.2. – Laser welding procedure

An Ytterbium Fibre Continuous Wave laser (YLR-6000-S) was used for laser welding of the specimens. Pure argon (99.997%) was used as shielding gas, and three shielding points with different gas flows were established in the assembly (Fig.1): 50 l/min in the nozzle to avoid fumes in the protection lens, 20 l/min to shield the weld pool and 30 l/min for the trailing gas shielding.

![Fig 1. Laser assembly showing the shielding locations.](image1)

![Fig 2. (Left)- Laser weld; (Right)- Transverse cross-section to evaluate](image2)

Some pre-trials were conducted in order to select the combination of laser variables with low (L) and high (H) energy inputs, which would ensure conduction and keyhole welding modes. Table 3 shows the final parameters and settings used for laser welding.

**Table 3- Parameters and settings used in the laser welding**

<table>
<thead>
<tr>
<th>Settings reference</th>
<th>Energy input (J/mm)</th>
<th>Welding mode</th>
<th>Coll (mm)</th>
<th>Foc (mm)</th>
<th>Foc +/- (mm)</th>
<th>Spotsize in focus (mm)</th>
<th>Fiber (μm)</th>
<th>Nominal Power (W)</th>
<th>Welding speed (mm/s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>L</td>
<td>45</td>
<td>Conduction</td>
<td>200</td>
<td>160</td>
<td>4</td>
<td>0.48</td>
<td>600</td>
<td>1800</td>
<td>40</td>
</tr>
<tr>
<td>H</td>
<td>110</td>
<td>Keyhole</td>
<td>160</td>
<td>200</td>
<td>6</td>
<td>0.75</td>
<td>600</td>
<td>2200</td>
<td>20</td>
</tr>
</tbody>
</table>

The optical settings (fiber diameter, collimating, focal length and out of focus) are in close relation to the width of the weld, and they are mathematically related by the expression: (focal length/collimating) x fiber diameter = spot size

2.3. – Cooling rate determination

A heat transfer and liquid metal flow model was used to calculate temperature fields and cooling rates for the welding of 316L stainless steel. The model has been extensively tested for the keyhole mode welding of Ta, Ti-6Al-4V, V, 304 stainless steel, a structural steel and Al-5754 aluminium alloy for various combinations of welding speed and power [23-26]. The different shape and size of the weld pool for different materials and welding conditions has been shown to be satisfactorily predicted by the model. Welding conditions also represented different heat transfer mechanisms, i.e., conduction and convection dominated heat transfer modes in the weld pool. Computational efficiency of the numerical model was achieved by assuming a quasi-steady state behaviour of the keyhole shape and the flow of heat and liquid.
metal in the weld pool. A detailed description of the model is available in the literature [23-26].

R-type thermocouples (Platinum-13%Rhodium/Platinum) were used for experimental determination of the cooling rate using a sampling rate of 333 Hz. Samples of 316L type stainless steels were prepared reproducing the shape and dimensions of the experimental alloys. Holes with a diameter of 1 mm diameter were drilled 0.5 mm below the surface and the thermocouple wires (0.2 mm diameter) were inserted.

2.4. – Characterization of microstructures

Once the specimens were laser welded they were cut and the transverse cross-section (Fig. 2) was ground and polished according to standard metallographic preparation procedures. The etchants used were Lichtenegger-Blöch (at 35-40ºC between 3.5 min. to 4 min.) and electrolytic etching (40% NaOH at 5V). Optical microscopy (Leitz Aristomet equipped with a Leica DFC 420 camera) was used for microstructural characterization. For dendrite arm spacing measurement, the UTHSCSA Image Tool software version 3.00 (developed at the University of Texas Health Science Center in San Antonio, USA) was used.

3- Results and discussion

3.1. - Cooling rates

Table 4 summarises the cooling rates obtained with the computational model for the centre-line and the fusion boundary at the surface of the laser weld. As an example, Figure 3 presents the computed temperature and the velocity fields on the top surface of the weld for the higher energy input settings (H) under steady-state conditions. The lower energy input weld (L) experienced significantly more rapid cooling at all temperature ranges compared to the weld produced with the higher heat input (H).

<table>
<thead>
<tr>
<th>Area</th>
<th>Settings</th>
<th>Above liquidus (ºC)</th>
<th>Solidf. (ºC)</th>
<th>Below solidus (ºC)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>1827-1727</td>
<td>1727-1410</td>
<td>1410-1357</td>
</tr>
<tr>
<td>Centre-Line</td>
<td>H</td>
<td>-11.870 ºC/s</td>
<td>2.485 ºC/s</td>
<td>392 ºC/s</td>
</tr>
<tr>
<td></td>
<td>L</td>
<td>-32.000 ºC/s</td>
<td>6.380 ºC/s</td>
<td>874 ºC/s</td>
</tr>
<tr>
<td>Fusion</td>
<td>H</td>
<td>----</td>
<td>----</td>
<td>4.045 ºC/s</td>
</tr>
<tr>
<td>boundary</td>
<td>L</td>
<td>----</td>
<td>----</td>
<td>9.360 ºC/s</td>
</tr>
</tbody>
</table>

Several measurements of cooling rates were made for each energy input level, and representative examples are presented in Table 5 (experiments A to D). There was a significant scatter in the results as the small weld pools made it difficult to ensure that temperature was measured in exactly the same location repeatedly. The introduction of the hole for insertion of the thermocouples was also a complication as this affected the local geometry of the weld pool. Furthermore, it is also known that under rapid variations of temperature, the inherent thermal inertia of the thermocouples can introduce a significant error in the measurements.
Figure 3- Computed temperature and velocity fields on the top surface of the laser weld for high energy input settings.

Table 5- Experimental cooling rates

<table>
<thead>
<tr>
<th>Settings</th>
<th>Experiment Ref.</th>
<th>Below solidus (ºC)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>1357-1127</td>
</tr>
<tr>
<td>H</td>
<td>A 1.4E+3 ºC/s</td>
<td>1.5E+3 ºC/s</td>
</tr>
<tr>
<td></td>
<td>B 1.6E+3 ºC/s</td>
<td>1.2E+3 ºC/s</td>
</tr>
<tr>
<td></td>
<td>C 2.8E+3 ºC/s</td>
<td>3.1E+3 ºC/s</td>
</tr>
<tr>
<td>L</td>
<td>D 1.4E+4 ºC/s</td>
<td>6.6E+3 ºC/s</td>
</tr>
</tbody>
</table>

In spite of all experimental difficulties there was a reasonable agreement between calculated and measured values. It can be seen that the cooling rates below solidus is on the order $10^3$ ºC/s for the higher energy input and about $10^4$ ºC/s for the lower energy level.

A comparison between the experimental cooling curves obtained with the thermocouples and the calculated cooling curves for the fusion boundary are shown in Figure 4. When comparing experimental and calculated results, it should be kept in mind that there is a difference in the location where the computational model calculates the cooling rate, which is at the top surface, and the location of the thermocouple, which is initially 0.5 mm under the top surface and laterally inserted through a drilled hole (Figure 5).

Figure 4- Comparison of calculated and measured cooling curves for the low energy input laser welds. Left: experimental curve. Right: simulated curve for the fusion boundary.
3.2. – Laser weld cross-section profiles

The transverse cross-sections of the laser welded specimens (Fig. 2) were inspected metallographically and the profiles of the welds were analysed to determine whether these suggested that welding was done in the conduction or the keyhole welding mode. Figure 6 shows representative examples of weld profiles confirming that the lower energy input resulted in conduction mode welding, whereas the higher energy input produced a keyhole. The width (W) and depth (D) of the welds were measured by image analysis with Image Tool software. Table 6 presents the results and the calculated W/D ratios. From these results it seems clear that for the lower cooling rate and keyhole mode; the W/D ratio is steady at around 1.4 for all alloys. However, for the higher cooling rate and conduction mode; the W/D ratio decreases as the \( \frac{\text{Cr}_{\text{eq}}}{\text{Ni}_{\text{eq}}} \) ratio of the alloys increases. It may be concluded that the weld profiles obtained for the lower energy input are less stable and might be sensitive to variations in the chemical composition of the alloy.

\[ \begin{array}{|c|c|c|c|c|c|c|} \hline \text{Alloy} & \frac{\text{Cr}_{\text{eq}}}{\text{Ni}_{\text{eq}}} & \text{Profile settings H} & \text{Profile settings L} \\ \hline & & \text{Width (mm)} & \text{Depth (mm)} & \text{Ratio} & \text{Width (mm)} & \text{Depth (mm)} & \text{Ratio} \\ \hline 1 & 1.52 & 2.57 & 1.90 & 1.4 & 1.43 & 0.33 & 4.3 \\ 2 & 1.58 & 2.60 & 1.88 & 1.4 & 1.38 & 0.34 & 4.1 \\ 3 & 1.60 & 2.44 & 1.89 & 1.3 & 1.60 & 0.42 & 3.8 \\ 4 & 1.66 & 2.55 & 1.86 & 1.4 & 1.44 & 0.42 & 3.4 \\ 5 & 1.74 & 2.43 & 1.60 & 1.5 & 1.62 & 0.77 & 2.1 \\ 6 & 1.84 & & & & 1.62 & 0.72 & 2.3 \\ \hline \end{array} \]

3.3. – Dendrite arm spacing

It is well-known that dendrite morphology and in general grain morphologies become finer as heat is extracted at a greater rate [19-22]. Therefore, the primary and secondary dendrite arm spacing (PDAS and SDAS) of austenite and skeletal ferrite, and also the inter-laths distance of skeletal ferrite were measured by image analysis for both laser welding conditions. The results were compared with those of the base material, which was arc furnace prepared and cooled at 10 °C/s.

Table 7 and Figure 7 present the results of measurements for alloy 2, but the same trend was seen for all alloys. The microstructure is coarsest in the base material and finest in the laser welds produced with the lowest energy input. In increasing the cooling rate from 10 to
10^3 °C/s, the dendrite arm spacing decreases by a factor of around 6, and in increasing from 10^3 to 10^4 °C/s the size decreases by a factor of nearly 2. It is, as expected, confirmed that the higher the cooling rate, the finer the microstructure becomes.

Table 7- Dependence of dendrite arm spacing on the cooling rate

<table>
<thead>
<tr>
<th>Alloy 2</th>
<th>Cooling rate (°C/s)</th>
<th>δ-ferrite</th>
<th>γ-austenite</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>PDAS (µm)</td>
<td>SDAS (µm)</td>
<td>Inter-laths distance (µm)</td>
</tr>
<tr>
<td>Base material (arc furnace)</td>
<td>10 °C/s</td>
<td>28.52 +/- 10.76</td>
<td>16.26 +/- 2.69</td>
</tr>
<tr>
<td>Laser weld (H)</td>
<td>-10^3 °C/s</td>
<td>5.32 +/- 1.22</td>
<td>2.34 +/- 0.29</td>
</tr>
<tr>
<td>Laser weld (L)</td>
<td>-10^4 °C/s</td>
<td>3.09 +/- 0.60</td>
<td>1.44 +/- 0.24</td>
</tr>
</tbody>
</table>

Figure 7- Micrographs of alloy 2. Left: base material (cooled at 10 °C/s), ferrite is coloured. Centre: laser weld H (cooled approx. at 10^3 °C/s), ferrite is shown white. Right: laser weld L (cooled approx. at 10^4 °C/s), ferrite is shown white.

3.4. – Solidification modes

In a previous study the transition AF-FA was observed at Cr_eq/Ni_eq ratios between 1.28 and 1.32 for a cooling rate of approximately 10 °C/s [14]. As expected, all alloys (Table 2, Cr_eq/Ni_eq from 1.52 to 1.84) showed FA solidification after cooling at 10 °C/s. They all contained skeletal ferrite and lathy ferrite, which are the typical ferrite morphologies of austenitic stainless weld metals during FA solidification.

According to the predictive diagram proposed by Katayama for rapid cooling conditions [15], alloys 1 and 2 should be fully austenitic A, alloy 3 AF, alloy 4 should show transition AF-FA and alloys 5 and 6 should be fully ferritic F. However, the experimental results demonstrated that the transition between primary ferritic and primary austenitic solidification does not occur at a single Cr_eq/Ni_eq ratio. All the alloys showed coexistence of regions with primary austenitic solidification and regions with primary ferritic solidification for both laser welding settings.

As solidification modes changed when the alloys were cooled rapidly after laser welding, it is clear that the transition takes place at higher Cr_eq/Ni_eq values at higher cooling rates. Comparing the same alloy, the presence of austenite is more evident in the lower energy laser welds, experiencing the highest cooling rate. For example, alloys 1 and 2 (Cr_eq/Ni_eq 1.52 and 1.58) show AF-FA coexistence in the higher energy laser weld (H). However, for the lower energy input (L) dendrites of pure austenite are also found. Similarly, in alloy 4 (Cr_eq/Ni_eq=1.66) coexistence is found between A/AF/FA for the lower cooling rates (H), but
for the highest cooling rate (L) there is also found Widmanstätten austenite which suggests fully ferritic solidification (F). The highest content of Widmanstätten austenite is found at the highest Cr_{eq}/Ni_{eq} values (1.74, 1.84) and at the highest cooling rates (L). Figure 8 presents some examples of coexistence of solidification modes in the alloys.
Under typical arc welding cooling conditions, an increase in the Cr\textsubscript{eq}/Ni\textsubscript{eq} ratio promotes primary ferritic solidification (FA, F). However, at the high cooling rates studied here, it was observed that by increasing the Cr\textsubscript{eq}/Ni\textsubscript{eq} ratio, the proportion of skeletal and lathy ferrite (suggesting FA) decreases and the eutectic ferrite morphology (suggesting AF) increases. A shift in the solidification mode to primary austenitic under rapid cooling conditions has earlier been documented in several studies [9-13]. The stability of austenite as primary solidification phase increases compared to ferrite because of the increased dendrite tip undercooling. In this work both primary austenite with eutectic ferrite (AF) and in a few cases also primary austenite dendrites (A) were observed. The primary austenite dendrites are found at the fusion boundary of the alloys with lower Cr\textsubscript{eq}/Ni\textsubscript{eq} ratios. The specimens with higher Cr\textsubscript{eq}/Ni\textsubscript{eq} ratios also form Widmanstätten austenite as a result of a primary ferritic solidification (F) and the solid state transformation reaction $\delta \rightarrow \gamma$. Surprisingly the highest content of Widmanstätten austenite is found at the highest Cr\textsubscript{eq}/Ni\textsubscript{eq} values (1.74, 1.84) and at the highest cooling rates. This will need further studies as higher cooling rates would normally be expected to result in less formation of Widmanstätten austenite.

In conclusion, when increasing the Cr\textsubscript{eq}/Ni\textsubscript{eq} ratio from 1.52 to 1.84, the presence of austenite increases due to two different mechanisms. A higher stability of austenite due to an increasing dendrite tip undercooling at higher cooling rates promoted austenite formation at the lower Cr\textsubscript{eq}/Ni\textsubscript{eq} ratios. On the other hand, at the highest Cr\textsubscript{eq}/Ni\textsubscript{eq} ratios, Widmanstätten austenite formed by a solid state transformation after fully ferritic solidification.

### 4- Conclusions

- At higher cooling rates, the transition between primary ferritic and primary austenitic solidification takes place at higher Cr\textsubscript{eq}/Ni\textsubscript{eq} values. However, the transition does not occur at a single Cr\textsubscript{eq}/Ni\textsubscript{eq} ratio and all the alloys (Cr\textsubscript{eq}/Ni\textsubscript{eq} from 1.52 to 1.84) showed coexistence of primary austenitic and primary ferritic solidification for both laser settings.
Comparing the same alloy, the presence of austenite is more evident in the laser weld with the lower energy input.

- Austenite formation was governed by two different mechanisms. At the lower Cr<sub>eq</sub>/Ni<sub>eq</sub> ratios the increasing dendrite tip undercooling at high cooling rates promoted austenite formation. On the other hand Widmanstätten austenite formed by a solid state transformation following fully ferritic solidification at the highest Cr<sub>eq</sub>/Ni<sub>eq</sub> ratios.
- It is confirmed that the higher the cooling rate, the finer the microstructure. When increasing the cooling rate from 10 to 10<sup>3</sup> °C/s, the dendrite tip spacing decreases by a factor of around 6, and in increasing from 10<sup>3</sup> to 10<sup>4</sup> °C/s with a factor of nearly 2.
- There was a reasonable agreement between calculated and measured values. The cooling rates below solidus is on the order 10<sup>3</sup> °C/s for the higher energy input and about 10<sup>4</sup> °C/s for the lower energy level.

**Acknowledgements**

Zhuyao Zhang at Metrode Products Ltd is gratefully acknowledged for the supply of the GTAW consumables and for the permission to use their electric arc remelting furnace in the preparation of the alloys. Eva-Lena Bergquist at ESAB AB is also gratefully acknowledged for her assistance during the metallographic preparation of the specimens. Kjell Hurtig at the Department of Engineering Science at University West is especially thanked for his contribution and involvement in the laser welding and the cooling rate measurement experiments.

**References**

LASER WELDING OF 1900 MPA BORON STEEL

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Abstract

A new category of boron alloyed advanced high strength steel (AHSS) is entering the market since a couple of years which give great potential for the automotive industry. The driving force is lighter structures with thinner material thicknesses. Conventional boron steel used in the automotive industry reaches around 1500 MPa while the new boron steel goes up to 1900 MPa in strength, allowing thinner gauges for the same construction strength properties. To achieve such properties the material is highly alloyed with carbon (0.34 wt%). This is results in that the welding scenario is predicted to be influenced by cracking and other unwanted phenomena’s. Laser welding in hardened condition, simulating assembly line, shows that cracking and other defects can be avoided i.e. welding quality similar to welded conventional boron steel can be achieved.

Keywords: laser welding, boron steel, cracking, 1900 MPa, automotive industry, lightweight, AHSS, assembly line

1 Introduction

In the automotive industry there is a continuously on-going struggle to minimize the weight of the vehicles, without hazarding crucial safety requirements, in order to reduce the “carbon footprint” on the environment [1]. Therefore, numerous variants of high strength steels are introduced in the car body, making it possible to down-gauge the different steel components without the renounce of important properties such as strength, stiffness and the ability to absorb high energy at impact situations.

One such material is the advanced high strength (AHS) press-hardened and hot-formed boron-alloyed steel. Apart from a unique process technology, when manufacturing automotive components in this material, it also contains specific alloy elements in order to guarantee a high ultimate strength. The drawback is that due to the alloy content these components become more difficult to weld. At various loading conditions, these welds also present a different behavior to what is common for welds applied in lower strength steel grades [2]. Therefore, the introduction of press-hardened body components not only affects the production conditions, but also the overall performance of the car body structure.

1.1 The Press-Hardening Process

Press-hardening or hot-stamping and die quenching is a method to produce ultra-high strength components for the automotive industry. It has been used since the mid-80’s, and typical components produced by the press-hardening process are e.g. A- and B-pillar reinforcements, floor sills, cant rails, side impact door beams and bumper beams. To enable the Boron-alloyed steel material to be formed and further on cooled down to a fully martensitic structure, the material first has to be heated up to its austenitisation temperature at around 880-950°C. To achieve a fully martensitic structure, the cooling rate must exceed 25-30°C/s. The small
amount of Boron (~ 0.002 weight-%) is used to facilitate the quenching process, wherefore the material often is referred to as Boron steel in colloquial terms.

The hot-forming process is mainly divided into two different approaches, the indirect and the direct one, where the direct process is the most commonly used among the automotive OEMs. These two processes offer different advantages for the final application in terms of design versus cost and available choices for surface protection. Making the first forming step(s) by conventional cold forming, the indirect process offers the possibility for more complex geometries and undercut designs before the shaped components are heated, transferred to the furnace and finally hardened (Figure 1). This means that two sets of tools are necessary, one for pressing and one for cooling. One advantage however, with this process, is that it offers the possibility to use a conventional zinc-based coating for cathodic corrosion protection.

![Figure 1. Schematic description of the indirect hot-forming process with the following steps: cold-forming of the blank in one or several steps, heating of the pressed part to roughly 900°C, rapid cooling of the part to achieve a martensitic structure, post processing like laser- or tool cutting etc.](image1)

In the direct process, the forming operation is integrated with the cooling operation in the same tool (Figure 2). This means that the parts reach their final shape and strength in one single press operation, and are therefore often referred to as "hot-stamped" or "hot-formed". The formed components remain in the dies until they have cooled down to between 100-200°C. All Boron-alloyed steel components in today's Volvo cars are manufactured using the direct process [3]. Most car applications such as door beams, bumper beams, sill reinforcements, A-, B- and C-pillars and roof bows can be produced with the direct process. The indirect process might, however, be necessary if more advanced parts are considered.

![Figure 2. Schematic description of the direct hot-forming process; heating of the blank to roughly 900°C, pressing of blank to final shape and consequent rapid cooling to achieve a martensitic structure in one single step. post processing like laser- or tool cutting etc.](image2)

The amount of Boron steel in body structures from Volvo Cars has increased from 3% in 2000 to 18% in 2010 [4], and exceeds 20% in conjunction with the launch of the company's latest model - the V40 small estate wagon (Figure 3). Fiat, VW and BMW show similar
figures today, but the difference is that today these companies have their own in-house production, whereas Volvo Cars so far purchase all Boron-alloyed parts from external suppliers. The upper limit for Boron steel in a car body is predicted to be around 45%, which more or less includes the whole safety cage with its backing structure. A rough estimation gives that an increase of Boron steel from 10% to 40% would reduce the body weight with approximately 30 kg with unchanged properties, i.e. stiffness, strength, crash and NVH (Noise, Vibrations & Harshness).

![Figure 3. Development trends regarding the utilization of press-hardened body components at Volvo Car Corporation.](image)

Laser welding is a common tool for assembly of different components. As described, if thinner and stronger components can be used, further weight savings can be done. This study addresses to investigate the weldability of a new grade of AHSS, MBW-K1900, which is predicted to be influenced by common weld defects due to strength increasing alloy components.

## 2 Lighter components using thinner material with higher strength

Advanced high strength steels (AHSS) is widely used within the automotive industry, and the usage is expected to increase. AHSS is suitable in front and rear bumper beams, door reinforcements, windscreen upright reinforcements, B-pillar reinforcements, floor and roof reinforcements, and roof and dash panel cross members [5, 6, 7]. The further striving for lighter structures is forcing the automotive producers to use innovative ways to achieve the wanted weight to strength ration of the design. Unchanged performance properties with thinner and lighter material are a critical issue that has been under great focus the past years. Researchers and material producers all over the world is looking for new ways of designing materials that have higher strength but still are suitable for structures in automotive industry. Following objectives are commonly targeted:
• Weight reduction
• Security increasing and crash improvement
• High formability
• Costs reduction
• Sustainability

2.1 MBW-K1900

MBW-K1900 is an AHSS highly alloyed with carbon that is produced by ThyssenKrupp Steel. The extra amount of carbon creates a harder material with higher strength than more conventional boron steels enabling possibilities for thinner gauges. As recognized in the name, MBW-K1900 attains a tensile strength of 1900 MPa. Table 1 shows the chemical composition of MBW-K1900 and the more conventionally used USIBOR 1500P.

Table 1. Showing the chemical composition of MBW-K1900 and USIBOR 1500P. The values are in W%.

<table>
<thead>
<tr>
<th>Chemical composition:</th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>C</td>
<td>Si</td>
<td>Mn</td>
<td>Al</td>
<td>Cr+Mo</td>
<td>Ti+Nb</td>
<td>B</td>
<td>CE</td>
</tr>
<tr>
<td>MBW-K1900</td>
<td>0.343</td>
<td>0.243</td>
<td>0.033</td>
<td>0.033</td>
<td>0.118</td>
<td>0.032</td>
<td>0.0027</td>
</tr>
<tr>
<td>USIBOR 1500P</td>
<td>0.22</td>
<td>0.3</td>
<td>0.032</td>
<td>0.032</td>
<td>0.193</td>
<td>0.039</td>
<td>0.005</td>
</tr>
</tbody>
</table>

The following equation has been used for calculating the carbon equivalent (CE). The equation corresponds to the IIW-recommendation.

\[
CE = C + \frac{Mn}{6} + \frac{Cu + Ni}{15} + \frac{Cr + Mo + V}{5}
\]

The CE is calculated to be around 0.57 for MBW-K1900 which is higher than for conventional boron steel. It is generally known that the CE should not be higher than approximately 0.5 to expect good weldability, although several other factors need to be considered as well to fully state the weldability. If welding steel with higher CE phenomenon such as hydrogen induced cold cracking can occur. The carbon content also affects the hardness of the material. Measurements done in the bulk of the material show an average hardness of ~600 HV0.5. See Table 2.

Table 2. Showing the Vickers hardness measurement done. 10 measurements were done in the bulk of the material. The average value was calculated.

<table>
<thead>
<tr>
<th>Vickers hardness (HV0.5):</th>
<th>Average:</th>
</tr>
</thead>
<tbody>
<tr>
<td>614 622 599 599 625 596 622 617 586 606</td>
<td>608.8</td>
</tr>
</tbody>
</table>

In the same way the hardness was measured in the base material before hardening giving an average value of 180 HV0.5. The hardness is strongly connected to the carbon content of the material and the relation is schematically described in Figure 4.
Figure 4. Schematically showing the hardness–carbon relation for quenched and non-quenched steels.

The higher hardness and tensile strength is also a result from the quenching cycle. Heating up to austenitising temperature with a following proper quenching will give a fully martensitic structure with wanted properties. How the quenching should be performed is schematically shown in Figure 5. The figure illustrates a CCT (continuous cooling transformation) diagram. Figure 6 is showing the resulting microstructure.

Figure 5. Showing the CCT diagram for quenching of austenitised steel.

Figure 6. Left image is showing the microstructure (ferrite-pearlite) for non-quenched steel. The right image is showing the microstructure (fully martensitic) for quenched steel.
3 EXPERIMENTAL

Different trials were done to evaluate the weldability of MBW-K1900. All trials were welded in hardened condition. The surface of the steel was blasted to remove surface remnants such as oxides from the hardening process. Laser welding in hardened condition should illustrate assembly of larger structures in production.

3.1 Laser welding in hardened condition

Welding trials were performed at Volvo Cars Corporation pilot plant in Torslanda, Sweden. Specifics regarding the equipment and the setup of the trials can be seen in Figure 7. The solid state laser was mounted in a robotized motion system.

Figure 7. Showing the setup and the equipment used for laser welding in hardened condition. No filler material was used during these trials. Compressed air was used as shielding of the process.

The welding was performed on lap joints between the 4 different possible combinations of 1.0 mm and 1.5 mm steel sheets: 1.0 to 1.0, 1.0 to 1.5, 1.5 to 1.0 and 1.5 to 1.5 mm.

Shear tensile tests and cross-tension tests were done. 25 mm weld length on a 48 mm wide coupon was tested at 10 mm/min. Shear tensile tests were performed within 24 hours as well as after 2 weeks to see if cracking occurs after time.

Macrographic images were taken to investigate the weld geometry. Also a crack susceptibility test was performed to investigate the sensitivity for solidification cracking. The test included to weld a seam 2 mm from the corner of a sample to 15 mm from the second corner; an angled linear weld. This is done with bead on plate. After welding the samples were bended along the weld seam to reveal the crack. The oxidized crack length was then measured.

4 RESULTS AND DISCUSSION

4.1 Welding in hardened condition

Different setups of parameters were used to find a proper welding scenario. The parameters giving most aesthetical welds with respect to weld bead width and height, root side, external defects and penetration is shown in Table 3. Showing the parameters giving most aesthetical welds in the different thickness combinations.
Table 3. Showing the parameters giving most aesthetical welds in the different thickness combinations.

<table>
<thead>
<tr>
<th>Parameters</th>
<th>1.0 + 1.0 mm</th>
<th>1.0 + 1.5 mm</th>
<th>1.5 + 1.0 mm</th>
<th>1.5 + 1.5 mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>Power</td>
<td>3800 W</td>
<td>3800 W</td>
<td>3800 W</td>
<td>3800 W</td>
</tr>
<tr>
<td>Weld speed</td>
<td>5.5 m/min</td>
<td>5.0 m/min</td>
<td>4.0 m/min</td>
<td>4.5 m/min</td>
</tr>
<tr>
<td>Focus position</td>
<td>0</td>
<td>0</td>
<td>2.5</td>
<td>1.5</td>
</tr>
</tbody>
</table>

For respective parameter setup a cross-section image was taken in a light optical microscope to see the internal structure and geometry. All four setups showed nice looking weld geometry with fully martensitic structure and a narrow HAZ. See Figure 8 to Figure 11.

Figure 8. Showing a macroscopic image of a cross-section in 1.0 mm to 1.0 mm.

Figure 9. Showing a macroscopic image of a cross-section in 1.0 mm to 1.5 mm.

Figure 10. Showing a macroscopic image of a cross-section in 1.5 mm to 1.0 mm.

Figure 11. Showing a macroscopic image of a cross-section in 1.5 mm to 1.5 mm.

Results of the tensile testing are summarized in Table 4. Maximum shear and cross-tension strength and elongation at fracture. The values shown are average values out of 10 samples. Homogenous values were obtained within each test. Shear tensile tests were performed within 24 hours as well as after 2 weeks to see if cracking occurs after time and therefore causes lower strength. Results show that the strength is within the same range for both tests, although the elongation increases when testing after 2 weeks. The increased elongation show that the ductility of the material increases by time or that softening occurs with remained strength. If looking at the cross-tension test one can see that the $F_{\text{max}}$ is as expected for all samples except 1.5 mm to 1.5 mm where a low value was obtained.
Table 4. Maximum shear and cross-tension strength and elongation at fracture. The values shown are average values out of 10 samples. Fmax (kN) shows the maximum strength, At mm (mm) shows the elongation at fracture, ε Fmax mm (mm) shows the deformation at fracture during cross tension.

<table>
<thead>
<tr>
<th></th>
<th>Shear within 24 h</th>
<th>Shear after 2 weeks</th>
<th>Cross tension</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Fmax kN</td>
<td>At mm</td>
<td>Fmax kN</td>
</tr>
<tr>
<td>1.0 + 1.0 mm</td>
<td>16.94</td>
<td>3.97</td>
<td>17.76</td>
</tr>
<tr>
<td>1.0 + 1.5 mm</td>
<td>20.6</td>
<td>5.23</td>
<td>18.56</td>
</tr>
<tr>
<td>1.5 + 1.0 mm</td>
<td>24.82</td>
<td>5.95</td>
<td>23.75</td>
</tr>
<tr>
<td>1.5 + 1.5 mm</td>
<td>27.88</td>
<td>5.49</td>
<td>26.5</td>
</tr>
</tbody>
</table>

Figure 12 shows schematic curves of the shear tensile and cross-tension tests. The behavior of the different samples is the same. The reason for lower cross-tension strength could partly be a consequence of the lower deformation of the sheets seen in Figure 13 and Figure 14.

Figure 12. Illustrating the schematic behavior of the shear tensile tests and the cross-tension tests. Note that the different tests are shifted ~1 mm on the x-axis to clearer show every single specimen.

Figure 13. Showing a deformed 1.0 mm sheet from a cross-tension test. The failure is in the outer weld metal.

Figure 14. Showing a non-deformed 1.5 mm sheet from a cross-tension test. The failure is in the interface.

If looking at the penetration one can see that the penetration depth varies within the different thickness combinations (Figure 15). The combination 1.5 to 1.5 mm has less penetration than the other thickness combinations. Also if calculating the heat input per mm thickness of the two joined sheets (a nominal value for the heat input, not dependent on thickness of the sheets) one can see that 1.5 to 1.5 has a lower relative heat inserted to the material (Table 5).
4.1.2 Crack susceptibility

One way of examining the crack susceptibility during solidification is to weld a seam 2 mm from the corner of a sample to 15 mm from the second corner; an angled linear weld. This is done with bead on plate. A sample is shown in Figure 16.

A crack is expected to be developed from the corner of the weld closest to the edge since the heat conductivity is restrained by the edge itself. The sample is then broken along the weld seam, and the oxidized length of the weld is measured. This length corresponds to the crack length created during the welding sequence. The results from the evaluation can be seen in Table 6. The results show that the thicker is more sensitive to cracking. A large spread is noticed indicating the difficulties placing the weld seam at the exact same position for each sample.

Table 6. Showing the results from the crack susceptibility test. A large spread is noticed indicating the difficulties placing the weld seam at the exact same position for each sample.

<table>
<thead>
<tr>
<th>Oxidized crack length (mm)</th>
<th>Average:</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.0 mm</td>
<td>8.1</td>
</tr>
<tr>
<td>1.5 mm</td>
<td>9.9</td>
</tr>
</tbody>
</table>

5 CONCLUSIONS

To achieve lighter components within the automotive industry, thinner gauges with higher strength needs to be implemented. A new grade from ThyssenKrupp Steel, MBW-K1900, gives more beneficial strength to weight ratio than more conventional boron steel. The study presented investigates the weldability of this new grade in hardened condition in combinations 1.0 to 1.0, 1.0 to 1.5, 1.5 to 1.0 and 1.5 to 1.5 mm in a lap joint. Laser welding
trials has been performed showing stable process behavior and strength values between 17 and 28 kN for the 25 mm long welds. Low cross tension strength is shown in 1.5 mm to 1.5 mm joints. This is most likely due to that a lower relative heat is inserted into the material. Also crack susceptibility tests was done showing that thicker material is more prone to solidification cracking, although the results show large scattering.

6 ACKNOWLEDGEMENTS

Special thanks to Volvo Cars Corporation for making the welding trials, ThyssenKrupp Steel for material supply and the project group within “Centre for Joining and Structures” for participation in discussions and funding of the project.

7 REFERENCES


INFLUENCE OF THE METALLURGY ON FATIGUE CRACK PROPAGATION IN WELDED HIGH STRENGTH STEEL JOINTS

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Abstract

A literature study of high strength steels, fatigue and fatigue assessment of welds has been conducted and is briefly presented in this paper together with experiments on fatigue crack growth rates of laser welded high strength steel. It is well-known that the fatigue life of welded joints is heavily dependent upon the surface geometry and welding defects because of crack initiation from the high stress concentrations associated with these types of welding flaws. However, the crack propagation through different weld zones of laser-welded high strength steels and the corresponding impact from the metallurgy is not fully understood.

The experiments comprise three-point bending fatigue tests on laser-welded high strength steel with machined surfaces. Measurement of the fatigue crack propagation rate transverse the weld and hence through the different metallurgy and hardness of the heat affected zone and of the weld can contain information on the impact of the metallurgy on the crack propagation speed. The influence of different high strength steel grades and of different welding parameters on the crack propagation and fatigue life is discussed.

Keywords: fatigue crack propagation, weld, laser, high strength steel, fatigue testing

1 Introduction

For many welded products fatigue is the main load situation to be optimized. Many new high strength steel grades have been developed that basically enable improved product design, particularly weight reduction. However, welding usually lowers the strength and fatigue life of steel which hinders the reduction of plate thickness for dimensioning. For fatigue life assessment mainly the geometrical aspects of a weld like the weld shape and its stress raisers have been studied and considered, while the impact of metallurgy on fatigue crack propagation is not well understood yet. Therefore the present study aims at better measurement and understanding of the influence of the metallurgy of laser welded high strength steel on fatigue crack propagation.

The first part of this paper is a review of the mechanisms affecting fatigue life of high strength steel after welding. A literature study on the current research in high strength steel and fatigue of laser welds was made and is presented in the beginning sections of this paper.
The second part of the paper describes a method for measuring fatigue crack propagation through laser welds with constant stress intensity factor. The method is discussed based on first measurement results.

2 State-of-the-Art of advanced high strength steels

Progression in high strength steel development has been made in recent years with more advanced microstructural properties to further enhance their strength. Fig. 1 shows the properties of conventional high-strength steel compared to advanced high strength steels (AHSS).

Fig. 1. Elongation as function of tensile strength for different high strength steel grades [1]

AHSS is a group of steel types consisting of dual phase (DP), transformation-induced plasticity (TRIP) and martensitic steel (MART) [2]. All AHSSs have yield strengths over 300 MPa and tensile strengths over 600 MPa [3], [4]. The types of steels discussed in this paper are all from the “first-generation”. “Second-generation” steels are austenitic with high manganese content [5]. New types of AHSS are now in consideration. These “third-generation” steels are expected to have a better combination of strength and ductility than the first generation while being less expensive than steels from the second-generation. To achieve this, the microstructural properties will have to be more tightly controlled [5].

2.1 Dual phase steel

Dual phase steel contains martensitic islands ingrained in a ferritic matrix, see fig. 2. The steel is useful for creating body-in-whites due to its high formability compared to traditional HSS with similar mechanical properties [6], [7]. It has been shown that the yield and tensile strength is dependent on the volume percent of martensite where a higher content raises the strength [8], [9]. The grain size and distribution of martensite controls the ductility [4]. DP steels are much used in the automotive industry, giving an incentive to develop steels with more favourable properties. Matlock and Speer reviews heating and cooling cycles that enhances material properties [5]. Baltazar Hernandez et al showed that a higher composition of Cr and Mn increases the resistance against softening in DP steels. [10]
2.2 TRIP steel

TRIP steels contain ferrite, bainite, retained austenite and martensite, fig. 3.

The TRIP steels have good straining properties which can be seen in fig. 4. When TRIP steels deform, the retained austenite is transformed into martensite thus hardening the steel and thereby increasing the ductility [2], [4]. TRIP steels can be modified to achieve the properties of third-generation high-strength steel by increasing the volume of retained austenite and by increasing the stability of the austenite, [5].

Fig. 2. Model of microstructure in DP steel. [11]

Fig. 3. Model of microstructure in TRIP steel. [11]

Fig. 4. Comparison of stress-strain curves for TRIP, DP and HSLA steels [2]
2.3 Martensitic steel

Martensitic steels have the highest hardness and strength of the AHSS with a possible tensile strength of >1500 MPa. The MART steel is composed of a lath martensite matrix with small amounts of ferrite and bainite. MART steel has low ductility which can be improved by tempering after quenching at the cost of some strength. Fig. 5 shows the properties of MART steels compared to mild steel. Martensite is developed by rapid cooling from austenite, often by quenching after hot rolling or annealing. The transformation changes the crystal structure from face-centered cubic to a body-centered cubic and the fast cooling prevents the diffusion of carbon atoms [4], [12].

![MART Stress-strain](image)

Fig. 5. Stress-strain comparison between mild steels and MART steels. The MART steels have much higher stress but lower strain. [2]

3 State-of-the-Art on fatigue in laser welds

It is well known that the fatigue life of welded joints is heavily dependent upon surface geometry or defects caused by the welding. The initiation then starts at the highest stress raiser present. A common place for initiation is the edges of the weld root and toe where the radius is a stress raiser. Larger radii give higher stress concentrations. Undercuts and flaws from lack of penetration are other typical phenomena which give rise to fatigue cracks.

3.1 Fatigue assessment methods of welded joints

There are a number of different approaches that are used to analyse fatigue life and fatigue strength. Some of these approaches will be covered briefly in this chapter.
**Nominal stress approach**
Nominal stress is a global approach which considers only macrogeometric flaws, thus ignoring the local stress raisers in the weld area. Generally nominal stress at the weld throat, \( \sigma_w \), can be calculated by dividing the acting force, \( F \), with the area of the weld throat, \( A_w \) [13]:

\[
\sigma_w = \frac{F}{A_w}
\]

The calculated stress amplitudes are then compared to an S-N curve for a similar design [14] which is designated with a FAT-class describing the stress that the component will have a 97.7% probability to survive in \( 2 \times 10^6 \) cycles.

**Structural hot-spot stress approach**
The structural hot-spot stress approach considers the stresses at structural stress raisers except those that arise at local weld effects. This approach is useful when nominal stresses cannot be calculated. Structural stresses are calculated by extrapolating stresses at reference points to the weld toe itself, fig. 6.

![Illustration of the hot-spot stress method](image)

**Fig. 6. Illustration of the hot-spot stress method [13]**

FEA is often used to determine the stresses [13]. Fig. 7 shows how the meshing of the FEA can be carried out to calculate hot-spot stresses.

![Meshes of hot spot stresses](image)

**Fig. 7. Meshes of hot spot stresses. a) relatively fine mesh, b) coarser mesh [13]**

The following formulas are used to calculate the stress. The subscripts of the stresses tell at which distance from the hot-spot it should be measured:
Fine mesh, linear extrapolation:
\[ \sigma_{hs} = 1,67 \cdot \sigma_{0,4r} - 0,67 \cdot \sigma_{1,0r} \]

Coarse mesh, non-linear extrapolation:
\[ \sigma_{hs} = 2,52 \cdot \sigma_{0,4r} - 2,24 \cdot \sigma_{0,9r} + 0,72 \cdot \sigma_{1,4r} \]

**Notch stress approach**
The notch stress approach assumes that the material is linear-elastic and the key idea is to determine the stress at the root of the weld when the weld root radius is set at \( r = 1 \text{ mm} \). IIW states that the method is valid for material thicknesses \( \rho \geq 5 \text{ mm} \) [13]. A radius of \( r = 0,05 \text{ mm} \) is often used on thinner materials [15], [16]. The method is only valid for fatigue failures at the toe or the root of the weld. A hot-spot stress investigation must be made at the weld toe to complement the notch stress approach.

**Stress intensity factor approach**
The crack propagation approach can be used together with other methods for calculating fatigue life. For welded joints it is common to only assess them by crack propagation calculation since the propagation represents a vast majority of the fatigue life. [17]

There exist three kinds of surface displacement cracking, fig. 8, where Mode I is the most common and therefore will be the only one treated here.

![Fig. 8. Surface displacement modes](image)

The stress intensity factor \( K \) can then be calculated with

\[ K = Y \cdot \sigma \sqrt{a} \]

Where \( Y = \) Geometry dependent function
\( \sigma = \) Stress acting on crack, usually calculated with nominal or hot-spot approach

The fatigue crack growth can be calculated by the Paris-law:

\[ \frac{da}{dN} = C_0 \cdot \Delta K^n \]
Where \( a = \text{Crack size} \)
\( N = \text{Number of cycles} \)
\( \Delta K = \text{Stress intensity factor range} \)
\( C_0, m = \text{Material constants} \)

### 3.2 Influence of microstructure on fatigue in laser welds

The crack growth speed in the weld metal and HAZ can be lower than in the base metal because of the zig-zag crack path through the weld which creates roughness induced closure. [18] Nakashima compared the growth speed of welded DP steel with welded conventional steel and found that the crack grows faster in conventional steel. [19] Farabi discovered that the influence of welding reduced fatigue life in DP960 steel more than in DP600 but the DP960 steel still had better fatigue properties than DP600 base metal in higher stress amplitudes. [20]

### 4 Methodology on measuring fatigue crack propagation

A three point bending specimen is produced with a weld transverse to the crack direction, fig 9. The crack length is then obtained by measuring the compliance with a clip gage. The growth rate is obtained by differentiation. The testing machines used have a maximum load of 20 kN and 250 kN. The fatigue specimen is 140 mm long, 30 mm high and 5.8 mm thick. During testing the stress intensity is kept fixed which causes the load to decrease when the crack grows larger.

![Fig. 9. (a) Experimental setup of the fatigue testing equipment and sample, (b) laser welded sample prepared for fatigue testing, including a notch and the horizontal laser weld.](image)

### 5 First results and discussion

A bead-on-plate weld was made on the S420MC test piece with a welding speed of 0.6 m/min and a power of 3.3 kW. The laser used was a 15 kW fibre laser. The crack propagation rate of the welded sample was then compared with the crack propagation rate of the base material (which was almost constant), the hardness and the cross section of the welded sample, see the graph in fig. 10. The x-axis shows the crack length of the test piece. The left y-axis is the growth rate per cycle while the right y-axis shows the hardness.
The results indicate a clear increase in crack propagation rate when going through the edge of the heat affected zones and also when entering the fusion zone. The propagation rate is slower in the warmer part of the HAZ near the fusion zone. The reason for the increase in propagation rate in the outer HAZ is believed to be caused by the change in microstructure occurring there. Fig. 10 shows a darker region where the propagation peak occurs. The rise in crack propagation in the middle of the weld is probably caused by tensile residual stresses from the welding operation. The areas between the propagation peaks with lower crack propagation rate may be the result of beneficiary material properties induced by the hardening during welding.

Fig. 11 shows the crack path from the top surface and also has the bottom surface overlaid on the top surface and shows that the crack grows with an inclination after passing through the weld.

Further tests must be conducted before any conclusions can be drawn.
6 Summary

- Fatigue cracking of welded joints is widely studied but mainly with respect to geometrical aspects, hardly for metallurgy.

- Some papers show different fatigue crack propagation speeds for different weld metallurgy zones where retardation can take place.

- For different high strength steel grades either harder or softer heat affected zones can be generated from welding and can be expected to give rise to different fatigue propagation speeds.

- A method has been studied to measure the fatigue crack propagation speed across the different metallurgy zones of laser welded high strength steel where different speed was observed; however, residual stress can have an impact; further studies are in progress.
Acknowledgements

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Laser Welding Applications with Multi Channel High Power Fiber Lasers

Laser Beam Parameter Set Up and Verification –

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01. Abstract

A multi channel fiber laser could feed up to four production lines or scan heads inside one or different laser cells. Modern laser welding applications must provide short cycle times and fast beam guiding. A four channel beam switch must ensure identical beam and process parameters at all four fiber outlets, respective fiber connectors.

PRIMES observation with a MicroSpotMonitor allows control and storage of all relevant data. The fiber will be connected to a collimation and or to a beam expansion unit. Different control routines before and after collimation enables easier set up.

Measurements in different distances give an overview regarding the collimated beams, the beam quality and the power density distribution before entering the focussing or working head. Each single optical component will be checked whether the possible impact on relevant optical parameters and finally relevant welding process parameters. The focus analysis finally mirrors the situation right in the tool center point.

The "before" and the "after" Beam Parameter Products will be measured and documented. The laser scan heads and adapted F-Theta lenses or even telecentric F-Theta objectives will be checked.

Measurement protocols will be shown, as well as a short overview of the used different diagnostics tools.

Keywords: Fiber adjustment, PMM, FocusParameterMonitor, MicroSpotMonitor, divergence

02. Introduction

Industrial applications of fiber guided NIR laser beam sources might have different ambitions for a multi channel set up. Sometimes two different processing heads, e.g. for welding will be needed. Sometimes a true alternating process, e.g. step weld left-right or simply one high power laser beam source will be used for two, four or even more different process zones.
The laser beam, provided by one single active fiber (feeding fiber) must be collimated, bended and focussed into up to 4 different processing fibers "on request".

The given Beam Parameter Product, coming out of the active fiber, should be preserved throughout the optical path from the feeding fiber into the working fiber and thereof beyond the fiber connector of the process fiber and following components like collimator, expander, scan head or any other focusing unit.
A large profit will be made if all resulting beam parameters will be equal, at least for equal process results.

03. Beam Path before Working Fiber

The feeding fiber is providing the "given" Beam Parameter Product. Sampling one dual fiber switch design will be analyzed in detail.

The outcoming beam must be observed at the working fiber outlet during alignment of the internal collimator and both of the focusing optics. The transmissive elements might allow a 3-D adjustment without a choice of tilting. The reflective elements should enable a 2-D alignment without any tilting. Some designs are using an adjustable fiber connectors for the working fiber. Multiplexing for more than 4 working fiber channels is used in special applications, only.

Fig. 02. Scheme of HP MSM HB
04. Observation at Working Fiber OUT

The HP MSM- High Brillance enables an accurate diagnostics under full power. Most welding and remote welding applications are using the power range between 4000 and 8000 W. A magnifying objective is watching the focus area. Here, the fiber outcoupling surface is taken as the focal plane. Observing the virtual ”inner” part of the caustic, inside the fiber and the divergent beam right after the working or guiding fiber enables an accurate determination of the BPP, the power density distribution, the symmetry and the all over adjustment. The High Power MicroSpotMonitor High Brilliance is measuring the throughcoming optical power as well. The video function enables online observation during alignment of all the participating axes as shown in fig. 01.

The fiber connector is mounted in a reference position on a bridge above the magnifying objective. This configuration is used for the first observation at the working fiber OUT position. Below the optical device the 10 kW absorber is visible and and will be completed with the precise power diagnostics.

Non- centric alignment between the incoming beam and the working fiber could reach extreme positions and values. Almost hundred percent of the optical energy could be guided into the cladding instead of the core (fig. 04).
05. Diagnostics Results at Working Fiber OUT

Fig. 04. Beam none centric at fiber OUT  
Fig. 05. Focus not adjusted at fiber OUT

The two axis alignment into the working fiber is most essential. Absolutely NOK is the “cladding mode” shown in fig. 04 except for special set ups to achieve a kind of ring mode. However, the longterm performance of the working /guiding fiber should be observed in such cases.

Fig. 06. Before - Higher Divergence  
Fig. 07. After - Minimum Divergence

The divergence could be enlarged by up to about 20 to 30% easily. Some collimating optics, welding heads or different types of scan headso still do not have any self protection against higher divergent beams, e.g. water cooled apertures defining the largest possible divergence e.g. 100 mrad or any other value, typically between 80 and 120 mrad.
Most important is indeed the resulting Beam Parameter Product (BPP) at the fiber outlet. The summarized propagation and the virtual beam enable the complete determination up to the far field region.

**Fig. 08. Near field – far field at fiber OUT**

Better than average specifications can be helpful for laser microwelding with reduced HAZ. This could be a "given" value if there is just a single outlet. Any multi channel application, beam switch or fiber extension will reduce the BPP. Here, the 100 micron fiber core diameter leads to 95 micron beam waist at the fiber end surface.

**Fig. 09. BPP of 2.77 mm*mrad**
Fig. 10. 3-D – Image BPP of 2.77 mm*mrad

Identical measurements must be processed under different optical loads to verify thermal effects and possible focus shift. If the values are within predefined tolerances and if absolute no impact by contaminations might occur, than the fiber connector should can be disconnected from the HP MSM HB and assembled to the collimator.

06. Observation after Collimating

Fig. 11. Slightly different but within tolerances
The resulting beam after collimating must match to the given clear aperture of the following beam path and optical components like lenses, mirrors or scan heads.

The specially designed BeamMonitor BM 60 NIR for the Near Infra Red can visualize the larger corona due to larger BPP or wrong collimating. The following sample shows a larger diameter than accepted here.

![Fig. 12. Corona after Collimating - NOK](image)

Up to 30 % of the total optical energy were observed in the corona in some applications. The resulting effects are well known: stray radiation, heating of optics mounts, poor cooling and finally process instabilities. But all resulting effects must be resolved far in advance. During the same measurement procedure fiber connector quality and collimator adjustment and centricity could be verified. Simply mounting a water cooled clear aperture to eliminate a corona or a non-coaxial beam and optics axis would lead to a poor solution which reduces the BPP and the symmetry which could be essential for 2-D applications.

![Fig. 13. Poor centering and beam angle at the fiber outlet](image)

![Fig. 14. Beam Path beyond fiber OUT down to Tool Center Point](image)
The ideal diagnostics tool is indeed the BM NIR which can detect expanded beams up to a clear aperture of 100 mm. Selectable detectors and measurement tips enable a perfect set up to a given power range and beam profile structure. The expanded dynamic range assist in visualizing higher peak intensities and larger corona or fringes due to unexpected divergence coming out of the fiber, wrong collimating or any alignment problems beyond the fiber outcoupling.

**Fig. 15. BeamMonitor for observation 2 and 3**

**07. Tool Center in the Process Zone**

After final focusing using a scan head for remote welding or any other laser welding or laser-hybrid welding head the focus diameter will be smaller or larger, depending on the imaging ratio given by the effective collimating and focusing optics. The HP MSM HB, formerly used for fiber switch adjustments will be mounted right underneath the focusing (working) head. Instead of the bridge with the fiber connector now the beam will be guided into the image.

**Fig. 16. HP MSM HB**
plane which will be controlled by the preselected MOB. Any focus diameter between 50 and 500 micron can be analyzed up to 6 or even 8 kW.

![Focus Caustic measured under a Remote Welding Scan Head](image)

**Fig 17. Focus Caustic measured under a Remote Welding Scan Head**

Ideally the BPP will be almost conserved but especially the aspherical F-Theta optics or more the telecentric F-Theta objectives will lead to a slightly increased BPP.

Scan Heads for remote welding allow faster processing, but this is one aspect, only. The core process might be different in details because proper gas guiding of process or shielding gas is nearly impossible. Some applications combines remote welding with a vacuum chamber, but this as a very special application.

The "big" advantage of the remote welding scan head is indeed the fast positioning of the focus. Some remote processing heads are moving the focus with about 10 to 80 m/s and have acceleration values up to 5 g or even more. At least the center position should be measured accurately. Different power levels between 10 and 100 % of the max. power enable tracking of different weaknesses or risky errors. Focus shift might occur or contaminated optics or debris shield windows. A valued side result is the power measurement in the processing zone. Comparison of feeding power in / out is a first hint to required cleaning.

**09. TCP Diagnostics**

One primarily parameter, the optical power is responsible for the seam cross section. A single pulse of slightly more than 100 ms enables the precise power measurement with the PMM. This PowerMeasuring Modul doesn’t need a human operator. Like a thermal or a flow sensor the arriving energy will be converted to the absolute power and documented.

![PMM ProfiNET](image)

*Fig. 18. PMM ProfiNET*
But just the power, with an unknown beam pattern or profile does not give the hint regarding process velocity or penetration. The FPM, the FocusParameterMonitor, includes such a PMM and the additionally beam profiling. The given focus dimensions, the power density distribution and the symmetry check complete the prime parameter for most welding and remote welding applications. This compact instrument communicates with the company network via ProfiBUS or ProfiNET. Details will be presented during 14th NOLAMP 2013.

Fig. 19. Scheme of FocusParameterMonitor

Fig. 20. FPM ProfiNET

08. References


COMPARISON OF LASER WELDING METHODS IN POSITION WELDING OF EDGE JOINT OF AUSTENITIC STAINLESS STEEL

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Abstract

An edge joint 2+2 mm thickness of ordinary austenitic stainless steel sheets was studied in all-position laser welding; flat (PA), vertical-up (PF), vertical-down (PG) and overhead (PE) positions. The penetration depth of the weld was aimed to be at minimum 3.0 mm. The optimal width of the weld was around 2 mm to guarantee adequate tolerances for the weld, but to avoid excessive melting of the edges of the sheets. In addition, the tolerance for the misalignment of the laser beam was tested. The welding procedure was tested with three laser welding methods, including de-focused laser beam, laser beam scanning and twin-spot laser beam welding in order to develop and find means to deploy laser welding robustly and reliably in practical applications. According to the results of welding test trials, the used configuration of twin-spot process seemed to be the most appropriate one among the tested welding methods to guarantee all position welding capability with constant suitable width and depth of weld penetration.

Keywords: twin-spot, laser beam scanning, laser welding, positional welding, edge joint, austenitic stainless steel

1 Introduction

The content of this research paper is associated with a case-study survey [1], where prospects of different laser welding methods to carry out the all position joining of 2 mm thick austenitic stainless steel sheets with edge joints of 3 mm penetration requirement were studied. The target application is comprised of rigid frame-like steel structure consisting of several meters of edge joint to be welded using robotic equipment. For the application point of view, it is highly important that an edge joint configuration can be sealed vacuum tightly with complete certainty at the whole length of the joint perimeter. As it is known, a laser welding at keyhole mode is inherently producing narrow weld fusion. That is why particular interest was laid on how weld fusion can be made a bit wider in order to relax the aligning tolerance of laser beam respect to joint center line.
2 Experimental procedure

There were three main factors which acted as a motivation of this study and simultaneously constituted the basis of requirement directed to the studied laser welding methods. The first one is that the used process has to be capable of performing in multiple welding positions with 3 mm penetration and melt pool control should be achieved accordingly. The second one is that the used process needs to be capable of withstand misaligning errors (horizontal beam off-set predominantly) occurring in the alignment of welding head respect to welded joint centre line and in the same time to be practically applied. The third criterion is that welding process should bring out low welding energy input in order to keep welding distortions at minimum.

Based on above requirements, the following laser welding process variations was chosen for try-out tests:

- De-focused laser beam
- Laser beam scanning
- Twin spot laser beam

Rectangular shaped (~450 x 100 x 2) mm AISI 316L sheets were used as test specimens. Penetration profiles from the test welds were determined with selection of cross-sectional macro graphs.

3 Results and discussion

3.1 De-focused laser beam welding trials

De-focused laser beam welding trials started with bead-on-plate (thickness 3mm) welding in flat position (PA), which purpose was check preliminarily proper range of welding speed to achieve 3 mm penetration using variable beam de-focusing values. The used laser source was 5 kW IPG fiber laser. The laser system enables a beam parameter product of 7.1 mm x mrad. Laser beam was guided to the Precitec’s welding head (focal length 150 mm) through a 200 μm fiber optic. The most promising parameter value range for focal position and welding speed was found to be as follows: F = -9…-15, v = 0.75…0.9 m/min. Maximum available laser power 4.5 kW at the surface was used. Furthermore, because welding was executed off the focal, it was decided to carry out subsequent trials with “dragging” beam configuration of 5 degree tilt from the normal to avoid possible back reflections which could be resulted from the use of de-focused beam. The widths of preliminary welds at surface and root side were evaluated to be in the range of 1.5…2.5 mm.

It was interpreted from the penetration profiles of preliminary welds that welding speed should be near 0.8 m/min or a bit less in order to secure 3 mm penetration requirement. What comes to focal point positioning, penetration profiles indicated that focal values of -9…-13 will serve as a proper range. For example, the use of focal value -11 will reserve ±2 mm beam positioning tolerance in vertical direction without affecting to the depth of weld penetration. Therefore value F = -11 at the middle of focal range was chosen to be a prospective constant.

In order to determine how tolerant used process is for horizontal positioning error of beam perpendicular to weld joint, following parameters were put in the further tests:

- Laser power 4.5 kW
- Welding speed 0.72 m/min (welding energy input, E ~ 0.38 kJ/mm)
- Focal position -11 mm below the surface
- Laser beam tilted 5 degree / dragging configuration
- Welding positions PA, PF, PG and PE

The path of the laser beam was intentionally aligned off the joint line as the misalignment offset values of 0.25 mm, 0.5 mm and 0.75 mm were tested, figure 2. The same misalignment configuration as shown in figure 2 was used hereafter also in scanning and twin-spot trials.

Figure 2. Sketch showing the set-up used in trials simulating horizontal positioning error of beam perpendicular to weld joint.

First set of trials were welded in PA position. The macro cross-sections taken from those test specimens can be seen in figure 3.

Figure 3. Weld cross-sections produced using de-focused beam and flat (PA) position. Used horizontal beam offset perpendicular to weld joint: a) 0.25 mm, b) 0.50 mm, c) 0.75 mm.

As judging from the penetration profiles of figure 3a…c, it can be concluded that in PA welding position used welding process can tolerate even the horizontal beam offset of 0.75 mm. Reduced (0.8 m/min -> 0.72 m/min) welding speed seemed to affect positively on weld penetration values as penetrations were in the range of 3.3…3.5 mm.

Figure 4, 5 and 6 shows the macro cross-sections taken from the test specimens welded in vertical up (PF), vertical down (PG) and overhead (PE) position, respectively. The penetration profiles in figure 4a…c show that the beam offset of 0.75 mm can be successfully
tolerated also in vertical up (PF) position, although some excessive penetration is generated as beam offset exceeds value 0.5 mm. The penetration profiles in figure 5a…c and 6a…c indicates that the beam offset of 0.5 and 0.75 mm can be successfully tolerated in vertical down (PG) and overhead (PE) position, if 3 mm penetration requirement is set as a judgment basement.

**Figure 4.** Weld cross-sections produced using de-focused beam and vertical up (PF) position. Used horizontal beam offset perpendicular to weld joint: a) 0.25 mm, b) 0.50 mm, c) 0.75 mm.

**Figure 5.** Weld cross-sections produced using de-focused beam and vertical down (PG) position. Used horizontal beam offset perpendicular to weld joint: a) 0.25 mm, b) 0.50 mm, c) 0.75 mm.

**Figure 6.** Weld cross-sections produced using de-focused beam and overhead (PE) position. Used horizontal beam offset perpendicular to weld joint: a) 0.25 mm, b) 0.50 mm, c) 0.75 mm.
As a summary from the executed de-focusing beam trials the following conclusion can be drawn:

- Welding can be carried out in all welding positions (PA, PF, PG and PE) with 3 mm penetration.
- Welding energy input \( E \) was calculated to be \( E \approx 0.38 \text{ kJ/mm} \) (4500W at 720 mm/min)
- De-focused process variation seems to tolerate at least \( \pm 0.5 \text{mm horizontal misaligning error respect to joint center line} \)
- For safety reasons, welding head is needed to be tilted from (at the current case 5 degree) the normal to avoid possible de-focused beam back-reflections into the optics. Dragging beam tilting seemed to be a suitable configuration, especially when welding in vertical down position.

### 3.2 Laser welding trials using a scanner head

This section reports laser welding tests, in which a scanner head is deployed to produce wider weld fusion and in that way achieve better tolerance against beam alignment perpendicular to joint center line. Used ILV DC scanner system was attached to Precitec laser welding head enabling linear laser beam scanning, figure 7. Laser beam scanning is achieved by the means of oscillating copper mirror inside the scanner head. Beam scanning parameters can be adjusted by pre-set sine wave control current. Main scanning parameters were scanning amplitude and frequency. In this application linear scanning transversal to welding direction was used. The same 5 kW fiber laser source as deployed in de-focus trials was used for the scanner head welding trials. The main welding parameters were as follows: Laser power \( P \) = 4.5 kW, focal length 150 mm and focal position \( F \) = -2, welding speed \( v \) = 1300 mm/min, scanning amplitude \( A_s \) \( \approx 2.8 \text{mm} \) and frequency \( f \) =150 Hz. To avoid back-reflections, welding head was tilted 5 degree and used as a dragging configuration. This is the same configuration as was used in de-focused beam trials.

*Figure 7. ILV DC-Scanner attached to the Precitec welding head.*
Scanner tests included set of trials where welding was tested in all positions (PA, PF, PG and PE) in order to validate the capability to produce 3 mm deep welds with reasonably wide cross-section profiles. Moreover, welding procedure was tested against misalignment offset (0…0.75mm) respect to joint center line.

The first set of trials was carried out in flat (PA) and vertical up (PF) position and the penetration profiles of those welds can be seen in figure 8 and 9, respectively.

![Figure 8. Weld cross-sections produced using laser beam scanning and flat (PA) position. The misaligned offset distance perpendicular to joint line was as follows: a) 0.25 mm, b) 0.50 mm and c) 0.75 mm.](image)

![Figure 9. Weld cross-sections produced using laser beam scanning and vertical up (PF) position. The misaligned offset distance perpendicular to joint line was as follows: a) 0.25 mm, b) 0.50 mm and c) 0.75 mm.](image)

Judging from the weld cross-sections presented in figure 8 and 9 (a/b/c), it can be concluded that in PA and PF position the penetration requirement of 3 mm can be reached and the laser beam misalignment offset of 0.75 mm can be tolerated. A track of beam scanning can be detected from the shape of solidified weld root portion. In figure 8a, it can be seen two small “peninsulas” indicating the utmost positions where laser beam has been positioned during the transversal scanning.

The following trial was carried out in vertical down (PG) position and during the welding it came obvious that melt pool was too large to be handled because gravitation was taken over leading to weld sagging, surface waviness and unsuccessful welding product. In
Figure 10, it can be seen the surface appearance from the vertical down weld SPG and the acceptable weld appearance of vertical up weld SPF as for comparison.

Figure 10. Weld surface appearances produced using a laser beam scanning procedure. On the left: weld sample welded in vertical down (PG) position. On the right: weld sample welded in vertical up (PF) position.

The third set of trials included trials which were carried out in overhead (PE) position. The penetration profiles of those welds are presented in Figure 11.

Figure 11. Weld cross-sections produced using laser beam scanning and overhead (PE) position. The misaligned offset distance perpendicular to joint line was as follows: a) 0.25 mm, b) 0.50 mm and c) 0.75 mm.

Judging from the weld cross-sections presented in figure 11 (a/b/c), it can be concluded that in PE position the penetration requirement of 3 mm can be reached if laser beam misalignment offset do not exceed the value of 0.50 mm.
As a summary from the executed laser beam scanning trials the following conclusion can be drawn:

- Welding can be carried out in PA, PF and PE welding positions with 3 mm penetration. Vertical down (PG) position produced unsatisfactory weld process because gravitation led to weld sagging.
- The used welding energy (E) was calculated to be E~0.21 kJ/mm (4500W at 1300 mm/min)
- Used laser beam scanning process variation seems to tolerate at least ±0.5mm horizontal misaligning error respect to joint center line.
- Because extra module for scanning is needed to be attached to the existing welding head, the size of the processing head is becoming quite a large. Therefore if described scanner procedure is considered as a welding tool for application with limited space in vicinity of joint to be welded, a “miniature” scanner/welding head has to be specially tailored for such application.

3.3 Laser welding trials using a twin-spot welding head

As a one tested alternative approach to get weld fusion profile wider was a twin-spot laser welding process. Twin-pot welding experiments were performed by using 3 kW Nd:YAG-laser (model HL 3006 D). The laser system enables a beam parameter product of 25 mm·mrad. Focusing optic with 100 mm focal length was used during the trials. An additional optical module was attached to the existing conventional laser welding head. The optical wedge-shaped quartz glass component (part 4 in fig. 12) divides one collimated laser beam into the two separate beams which are focused into the two parallel focal spots which center to center distance from the each other is 0.5 mm. The focus spots were perpendicular respect to welding direction. The schematic presentation of the principle and the configuration of above mentioned set-up are shown in figure 12.

![Figure 12](image)

**Figure 12.** The principle and optical set-up of the twin-spot welding optic used in the welding trials. 1. Laser light cable. 2. Laser beam. 3. Collimator. 4. Quartz glass wedge (twin-spot optic). 5. Focusing lens.
Used welding equipment set-up is shown in figure 13, where laser welding head with twin-spot module is attached to the wrist of KUKA KR125 articulated arm robot.

**Figure 13.** Used welding set-up in twin-spot trials. Welding head is attached to the wrist of KUKA robot. Location of twin-spot optic is pointed with red arrow.

At the start, preliminary bead-on-plate trials on 3 mm thick sheet and flat position were made to determine a proper parameter range concerning welding speed and needed laser power. Full penetration of 3 mm was achieved using the laser power range of 2.5…3 kW (this means 1.25…1.5 kW per each laser beam spot) and the welding speed range of 1.2…1.5 m/min. The widths of the test welds at the surface were measured to be ~2 mm.

Based on the results of bead-on-plate welds, the following trials were made in all welding positions (PA, PF, PG and PE) using welding speed of 1400 mm/min, laser power of 2.5 kW (1.25 kW per beam spot) and focal position of +0.5.

In figure 14, it is presented the compilation of macrographs of above mentioned trials. When examining the macrographs shown in figure 14, it can be concluded that 3 mm penetration can be achieved in all welding positions and the widths of the welds at the waist are around 1.1….1.4 mm. It can be also mentioned that rather high welding speed (1400mm/min) enables to weld with low welding energy in-put (E~0.11 kJ/mm). In this case, it also seems to be beneficial that the edges of the joint stay un-melted, which make melt pool control more robust than in cases where whole edge is being melted. Un-melted sheet sides support melt pool and it is not so susceptible to drift sideways during welding in multiple positions.

Finally, offset-trials testing the horizontal positioning error tolerance of twin-spot beam perpendicular to weld joint were carried out. Used welding parameters were the ones determined in the earlier trials: Laser power P= 2.5 kW, welding speed v=1400mm/min and focal position F= +0.5. During the earlier experiments, it was found that vertical welding position was the most challenging what comes to weld pool controllability, so offset-trials were decided to limit in PF position. The beam misalignment offset values of 0.25 mm, 0.4
mm, 0.5 mm and 0.75 mm were tested. **Figure 15** shows the compilation of cross-sectional macrographs from the above described horizontal beam alignment off-set trials.

![Figure 14](image1.png)

**Figure 14.** The appearance of cross-sectional macrographs welded with twin-spot process and in a) flat (PA), b) vertical up (PF), c) vertical down (PG) and d) overhead position (PE). Parameters: Laser power, \(P=2.5\ kW\); welding speed, \(v=1400\ mm/min\), focal position, \(F=+0.5\).

When examining the macrographs shown in figure 15, it can be seen that the used twin-spot process variant can tolerate horizontal beam off-set perpendicular to joint center line near to the range of 0.4 mm (fig 15b) whereas with the offset values of 0.50 mm (fig 15c) and 0.75 mm (fig 15d) a lack of fusion defect is emerged because part of the joint line has been left unmelted. However, in order to leave some clearance/back-up, it is recommendable not to exceed 0.3-0.4 mm horizontal beam misalignment to avoid lack of fusion defects. As above twin-spot result is compared to the tolerance range achieved in single beam keyhole laser welding, the horizontal tolerance of the beam off-set range in used twin-spot configuration can be estimated to enhance about 30-40% of what can be achieved using corresponding single beam configuration.
As a summary from the executed twin-spot laser beam trials the following conclusion can be drawn:

- Welding can be carried out in all welding positions (PA, PF, PG and PE with 3 mm penetration using one common set of welding parameters: Laser power 2.5 kW (1.25 kW per each beam spot), welding speed 1400 mm/min, focal position +0.5.
- The used welding energy can be sustained low because of rather high welding speed can be used. Welding energy (E) was calculated to be E~0.11 kJ/mm (2500W at 1400 mm/min).
- Used twin-spot laser process variation seems to tolerate near to the range of ±0.4mm horizontal misaligning error respect to joint center line.
- Compared to the other tested laser process variants in which the outer edges of joint is melted during the welding, it seemed to be beneficial for melt pool control as the outer edges of joint left un-melted as was the case in twin-spot welding. Un-melted edge sides are interpreted to serve as kind of “side supporters” for melt pool and therefor melt is not so susceptible to start drifting sideways in positional welding.
- The used twin-spot beam optical module is a ”fixed type”, meaning that the distance separating the two laser spot was fixed in 0.5 mm. Some further optimizing to get wider weld and better horizontal offset tolerance could have been made if the distance between spots could have been adjusted a bit wider.
4 Conclusions

An edge joint 2+2 mm thickness of ordinary austenitic stainless steel sheets was studied in all-position laser welding; flat (PA), vertical-up (PF), vertical-down (PG) and overhead (PE) positions. The welding procedure was tested with three laser welding methods, including de-focused laser beam, laser beam scanning and twin-spot laser beam welding in order to develop and find means to deploy laser welding robustly and reliably in practical applications.

If the process variations are contemplated against the criterions set, the used twin-spot process can provide welding in all (PA, PF, PG and PE) positions and melt pool behaviour is well controlled because the volume of molten pool created by keyhole welding can be kept small because joint edges do not get melted. Trials showed that twin-spot process with the configuration used was not so tolerant against horizontal beam off-set compared to other tested process variants. This is due to the smaller size (width and volume) of the produced melt pool compared to other processes. But on the other hand, the size of the melt pool should be defined as compromise between melt controllability (especially in vertical positions) and size of the weld width.

Defocused laser beam process provided welding in all positions (PA, PF, PG and PE), but welding energy (kJ/mm) in-put stayed rather large, because welding speed had to be lowered in order to achieve required penetration. In that sense welding deformations could cause problems if several meters of joint has to be continuously welded. Moreover, despite of the fact that horizontal beam off-set tolerance was found to be good in de-focused beam process because of wide melt pool, the flip-side is that the volume of molten pool will grow also as the width grows and melt control during the position welding will become more difficult.

Laser beam scanning process variant provided welding capability in PA, PF and PE positions, as vertical down (PG) position was not successful because melt pool with used configuration was unstable in that position. Horizontal beam off-set tolerance can be enhanced quite well using beam scanning and even with rather low welding energy in-put. One considerable down size for the use of a scanner process variation arises if there is limited space at the vicinity of joint area. Scanning unit enhances the size of the welding head and inherently it is not as compact as compared to e.g. a twin-spot module. Nevertheless, it can be seen that above issue could be addressed using a special miniature scanner welding head tailored to the used application in question.

Taking above discussed aspects and viewpoints into consideration, twin-spot process variation seemed to possess most appropriate capabilities what come to all position welding, melt controllability and reduced welding energy in-put. Nevertheless, some compromise on horizontal beam off-set tolerance capability has to be assented as the size of melt pool is recommended to keep small enough in order to secure melt control in multiple welding positions.

Disclaimer: The views and opinions expressed herein do not necessarily reflect those of the ITER Organization.

5 References

LASER HYBRID WELDING OF ALSI COATED BORON STEEL.

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Abstract

Several publications in the literature are stating that welding of AlSi coated boron steel without any pre-treatment often results in reduced weld strength and generally make the joint more brittle. These problems are normally solved by locally removing the AlSi coating in a separated process step before welding.

One of the main goals of the experiments here presented was to join tailor welded blanks (TWB) using only one process step without locally removing the AlSi coating and still achieve the required weld strength needed for subsequent forming operations and good crash performance.

Laser-hybrid welding, a mixture of laser and MAG welding, were used for welding of TWB’s

The experiments showed that correctly used, laser-hybrid can achieve welds in Usibor 1500P materials that reach 95% of the strength of welds made on uncoated 22MnB5 materials, in one process step without locally removing the AlSi coating.

Optimized laser-hybrid welds were also tested on full scale B-pillars where they performed as good as conventionally laser-welded B-pillars (with coating removal) and non-welded B-pillars. All welds were intact after both static and crash testing.

In all, the experiments has successfully gathered knowledge on the determining factors for a sturdy process with high weld quality, approved crash performance, and improved productivity.

Keywords: laser hybrid welding, AlSi coating, boron steel, tailor welded blanks
1 Introduction

An often used coating for press hardened parts in crash applications is the AlSi coating, which is also used in the commercially available coated Boron steel (Usibor 1500P). This and many other thicker coatings need local removal before laser welding to avoid process instabilities, reduced weld strength and a brittle joint [1]. For the welding of tailored blanks this reduces productivity and, when less successful results in poor joint quality.

Much is to gain if a more sturdy joining technology could be developed for tailored blanking, enabling direct joining without initial local removal of surface coating and with higher capability for rough cutting edges. These experiments aimed to do this development, and at the same time also reach improved quality, formability and crash performance.

2 Methodology

Plates made of AlSi coated Boron steel (Usibor 1500P) and uncoated boron steel (22MnB5), with milled and cut edges were laser welded and laser hybrid welded with a 15 kW Fiberlaser YLR15000 from IPG (200 μm fibre diameter, wavelength 1070 nm) combined with an ESAB MIG/MAG power source Aristo 450 LUD MIG/MAG. The laser was operated continuous wave, the MIG/MAG-arc in pulsed mode. The laser beam was focused by a 300 mm focusing lens to a focal spot diameter of 400 μm.

The plates with a length of 200 mm, a width of 100 mm and thickness of 1.4 mm, denominated (A) in Fig. 1a and Fig. 1b, were clamped on a fixture. The MIG/MAG torch (B) and the laser beam (C), with inclination angle of 7º moved along the joint with welding speed v. The process was observed by high speed imaging. The illumination diode laser optics (D) and the camera lens (E) can be seen in Fig. 1b.

![Fig. 1. Experimental setup. (A clamped samples, B torch, C laser beam axis, D illumination laser optics for high speed imaging, E high speed camera lens)](image)

Different laser powers, welding speeds and filler wires were chosen during the experiments with the aim to weld through the 1.4 mm thick plates. The MIG/MAG parameters as voltage, frequency, current, background current and pulse time were set.
according to the ESAB LUD 450 synergy lines and then adjusted with the aim to get a uniform weld at the top with no undercuts and a root that together could reach weld class B.

After welding, the samples were cut, polished and etched, to identify phases and to measure the cross section properties. Some of the samples were analysed in scanning electron microscope (SEM) performing electron backscatter diffraction (EBSD) to provide structural information, like crystal systems and phases, and by energy dispersive X-ray spectrometry (EDS) to provide with the chemical characterization of the weld. The tensile strength was measured for all of the welds.

3 Results and Discussion

Fig. 2 show the visual effects of the AlSi coating on the metallurgy in the weld as well as the effects on the tensile strength. Laser welds made on AlSi coated boron steel material, without coating removal show roughly a 30% drop in tensile strength compared to uncoated materials.

![Fig. 2. – Laser welds made on Usibor (AlSi coated) and uncoated boron steel](image)

EBSD and EDS measurements were made on laser-hybrid welds (C-steel filler wire) showing the same tendencies of formation of unwanted phases in the weld as the pure laser welded joint in AlSi coated boron steel material without coating removal. Results from these measurements are displayed in Fig. 3 and Fig. 4 below. The unwanted phase was identified as ferrite and the EDS composition values show that as much as 4.75% aluminium was detected in the ferritic areas of the weld joint.
Fig. 3. – EBSD and EDS measurements of unwanted areas in a laser-hybrid welded AlSi coated boron steel material. Images from SEM are mirrored compared to the light optical microscope image to the left.

![Fig. 3](image)

<table>
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<th>Si</th>
<th>Cr</th>
<th>Mn</th>
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All results in weight%

Fig. 4. – Composition values obtained from the EDS measurements

Thermodynamic calculations were used to better understand the influence of the AlSi coating on the metallurgy of the welds in AlSi coated boron steel. Results show that already after solving 1wt% aluminium into the steel, the ferrite phase starts to form at hardening temperatures, Fig. 5. If any ferrite is present during hardening of these steels the strength will be heavily reduced due to insufficient martensite formation.

![Fig. 5](image)

Fig. 5. – Thermo-Calc calculation of 22MnB5 material (uncoated boron steel) at hardening temperature showing the evolution of phases with increasing Al-content.
Additional thermodynamic calculations were performed for the experiments where an austenite stabilizing filler material was used during laser hybrid welding. The calculations showed that by adding an austenite stabilizing filler material during welding a larger amount of aluminum can be tolerated in the steel without the formation of ferrite. Fig. 6 show that with a hypothetical mixing rate of 50%, 3wt% aluminum can be dissolved into the steel without any formation of ferrite at hardening temperatures. This is three times more than for the pure laser welding case.

**Fig. 6.** Thermo-Calc results of base material mixed 50/50 with an austenite stabilizing filler material showing the evolution of phases with increasing Al-content.

The knowledge gained from the initial mechanism study was used to help improve weld strength of laser-hybrid welds in AlSi coated boron steels. Fig. 7 shows tensile test results obtained from pure laser welding in AlSi coated boron steel and Laser-hybrid welding using non optimal process setup and filler materials.

**Fig. 7.** Tensile strength of welds made in AlSi coated boron steel using laser welding and laser-hybrid welding with non-optimal process setup and filler materials.
When optimizing the process setup and using the correct filler material, laser-hybrid welds can reach up to 95% of the tensile strength of welds made in uncoated boron steels as shown in Fig. 8. An increase by roughly 25% compared to pure laser welding.

Fig. 8. – Tensile strength of welds made in AlSi coated boron steel using optimized laser-hybrid welding.

As seen on the cross section images below, Fig. 9 and Fig. 10, the formation of ferrite in the weld can be totally prevented by the use of an optimized laser-hybrid welding process thus minimizing the reduction of weld strength. Fig. 9a displays a pure laser weld where the small white areas in the weld are ferrite. Fig. 9b displays a laser-hybrid weld where the white areas in the root area also are ferrite. The upper part, however, is a mixture of the base material and the filler material and it doesn’t contain ferrite. The area is white due to the elements in the stainless filler material that prevents the material to be etched.

Fig. 9. – Cross section images from welds made in AlSi coated boron steel using pure laser welds and laser-hybrid with non-optimal process setup

With optimized laser hybrid parameters, Fig. 10, a complete mix of base material and filler material can be achieved with none or very little amount of ferrite.
Fig. 10. - Cross section images from welds made in AlSi coated boron steel using optimized laser-hybrid welding

The knowledge gained from the experiments was used to manufacture demonstrators, Volvo V70 B-pillars in different test versions. B-pillars were manufactured using conventional laser welding with coating removal, optimized laser-hybrid welding and without welding. Results from crash and static testing showed that the laser-hybrid welds perform as good as laser welded (AlSi ablated) B-pillars and conventionally manufactured non-welded B-pillars. All welds were intact after testing, Fig. 11.

Fig. 11. – Crash-tested B-pillars with AlSi-coating. The laser-hybrid welds are intact. Left image: test No 13; Right image: test No 5.

4 Conclusions

- The mechanisms behind weld strength reduction in laser welds of AlSi coated boron steel materials are now fully understood and are related to ferrite formation in the weld.
- AlSi additions to boron steel weldments promote the formation of ferrite at hardening temperatures resulting in poor weld strength.
- Laser-hybrid welding of AlSi coated boron steel materials using an austenite stabilizing filler material prevent ferrite formation thus achieves weld strength comparable to base material strength values, without removal of the AlSi coating.
- Tensile strength of optimized laser-hybrid welds in Usibor 1500P (AlSi coated) materials reach 95% of the strength of welds made on uncoated 22MnB5 materials.
- Laser-hybrid welds were optimized to reach weld class B with a high success rate.
- Static and dynamic tests of Volvo V70 B-pillars showed that laser-hybrid welded B-pillars performed as well as laser welded (AlSi ablated) B-pillars and conventionally manufactured non-welded B-pillars. All welds were intact after crash and static tests.
5 Acknowledgements

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Laser ablation for hardening laser welded steel blanks, Industrial Laser Solutions,  
March/April 2012,
Abstract

Laser additive manufacturing (LAM) in this paper, is a powder bed fusion process, where parts are created by melting metal powder layers into solid metal dense parts layer by layer according to sliced 3D-CAD model data. One challenge in the process, as complex geometries are built, is need of support structures to avoid the failure caused by overhang features (< 45°).

One the constant endeavor in the way to increase productivity of LAM is to reduce the support volume to minimum and designing optimal support structures suitable for complex geometries. The aim of this study is to gather information about previous studies about support structures used in metal-based LAM and based on this to give an overview about latest developments about topic. The experimental part consists of manufacturing different predesigned support structures. It is carried out in Laboratory of Laser Processing at Lappeenranta University of Technology using LAM setup with a fiber laser of IPG 200 W SM CW. Material used in this study is martensitic stainless steel powder EOS PH1.

Three support structures are designed and manufactured, namely cone-like, X-like and tree-branch-like support structures. Tree-branch-like support structures show combination of low volume, good repeatability and possibility to vary the support structure in a wide range of relative densities. It is suggested to design additional branches for this support structure, resulting in higher reduction of support structure volume and easier part removal. However, in the same time it will be suitable for manufacturing with LAM and in correlation with supported part volume.

Keywords: Laser additive manufacturing, overhang feature, support structure, tree-branch like supports
1 Introduction

Laser additive manufacturing (LAM) is a materials joining process for producing of three-dimensional (3D) parts by effective melting of metal layers. This is differing from conventional machining, where the material is removed by a machining process to obtain the desired shape. The difference in comparison to conventional machining is gaining the final part without any tool contact (as it is done in subtractive processes as turning and milling), resulting to production without tool wear and therefore no additional expense for tool sharpening or tool replacement.

There exist different classifications of LAM methods, among others Solid Freeform Fabrication (SFF) [1] and Direct Metal Laser Sintering (DMLS) [2]. The most common LAM method is called Selective Laser Melting (SLM, see Fig. 1).

![Fig. 1. Laser additive manufacturing (LAM), principle of most common method Selective Laser Melting (SLM) [3].](image)

At first, the 3D-CAD model is distributed into layers, after which the sliced data is transferred to SLM machine. Thereafter, the powder material with a thickness of one layer is laid on the building platform. The geometric information of the layered 3D-CAD model is transmitted by laser beam onto the powder bed. Regions containing solid material are scanned and melted with laser beam in inert atmosphere, which results in a solid layer of the part. With lowering the building platform by one layer thickness, the process steps are repeated. As a result, an almost fully dense part with no need for post-processing other than surface finishing is received [3, 4].

2 Support structures in LAM

2.1 Residual stresses in LAM

Residual stresses and distortions appear in metal-based LAM due to localized heating, complex thermal and phase transformation stresses [4]. According to [5-8], residual stresses and distortions are also dependent on the laser melting strategy.

There exist two different types of residual stress according to the scale they occur [9], namely macro and micro stresses. Macro stresses vary over large distances over the part, which result in partial deformation of the part. Micro stresses occurring at atomic scale (inside the grains) are less important for strength properties of material [9, 10]. In LAM, we consider the demand for support structures due to macro stresses.
For metallic components, where there are more residual stresses causing distortions and cracks, it is essential to design geometry specific supports with as low volume as possible. When support structures are in irrelevant relation to the structure of the material (removal of the support depends on the material mechanical properties), and orientation, it can lead to defective support structure build, distorted supported region or premature process stop [10].

2.2 Support structures in LAM

Support structures used in metal-based LAM have two functions. First function is to conduct the heat away especially from overhanging part areas to the building platform. Second function is to protect part sections against displacement during manufacturing. However, support structures are undesirable because the removal is, especially in case of complex geometries, complicated and after removal the value of detached supports is minimum [11].

Today, the support structures can be generated either manually, designing geometry specific support structures or automatically. Automatic support generation program is for instance suitable for support generation for periodic lattice structures. With these supports it is possible to fulfill the support structure function or use them as interface between structure and building platform (for cutting part later with electro discharge machining, EDM). The generative useful supports are (listed in Fig. 2): Point support structures (a) are suitable for supporting different lattice structures with relatively small strut diameter and also conical parts due to circular facet in bottom surface. With gusset support structure (b) it is possible to avoid part failure due to overhang feature. Block-like supports (c) are desirable for their easy removal from main part, leaving minimum marks on part surface.

![Fig. 2. Automatically generated support structures [12].](image)

Currently, most of previous studies about support structures have focused on non-metallic materials. The supports of the non-metallic component generally are easily removable without major effort. For instance, support structures used in Fused Deposition Modeling (FDM) process are water-soluble and the removal of thin wall supports in Stereolithography (SL) does not affect the supported region. Compared with previous supports used in additive manufacturing (AM) of plastic materials, removing metallic support structure from components distorts the surface more significantly. It is important to design an optimal support structure having low volume, opened cell geometry and symmetric design when connecting the support structures with each-other [10, 11].

Studies about cellular support structures using Selective Laser Melting (SLM) has been carried out by [12]. It is mentioned that the main advantage for cellular support structures is their low volume. It has opened cell geometry, which is also desired when generating structures. It is easy to remove the raw powder from opened cellular geometry. However, these cellular support structures (Schoen gyroid and Schwartz diamond unit cell types, see Fig. 3) cannot be perfectly repeatable. This means, that the interlocking geometry (unit cells of support structure) when connecting together does not cover the supported region continuously resulting high surface roughness $R_s$ in unsupported regions.
Also, the minimum cell size for cellular support structure is 2.5 mm (relative density 15%). Due to complex support structure geometry presented in Fig. 3, it is hard to obtain smaller cell widths having higher relative densities. This shape cannot be successfully produced with LAM in a smaller scale. However, smaller support structure unit cells mean better mechanical properties. Choice of support structure dimensions must be regarded with supported area dimensions.

Consideration of support structures have to take into account its manufacturability, removal, volume, manufacturing time and surface quality of supported area. Support structures are influencing the usage of material and total build time, surface finish, energy consumption and post-processing of manufactured parts [12]. Using conical geometry for support structure design is preferable due to high part supporting and heat conduction capability from supported region to building platform.

2.2 Support structure dependency on part orientation

Suitable part orientation according to deposition direction can improve part accuracy and surface quality, production time and support structure design. [13]. The geometry, amount and selection of support structures are dependent on the part orientation. It must be considered when designing new parts or when part redesign is required [14]. Part orientation affects the part building time. It means that taller parts take longer time for manufacturing than shorter parts [15]. Surface roughness value $R_a$ depends on part orientation (see Fig. 4). When manufacturing part $<45^\circ$ angle, support structures are necessary to be detached for part successful build with low surface roughness.

Fig. 3. Unit cell types Scheon gyroid and Schwartz diamond [12].

Fig. 4. The amount of support structure is dependent on part orientation [14].
2.3 Anchorless Selective Laser Melting (ASLM) process

Reduction or removal residual stresses and the requirement for support structures in SLM can be achieved by preventing parts from completely solidifying during manufacturing. A method for eliminating support structures has been invented by [15]. The part build without support structures is achieved with forming eutectic alloy or eutectic system (hyper/hypo eutectic) from two or more un-alloyed materials. This is done, when at the same time the powder bed heating is maintained above the newly formed eutectic melting (solidification) point (see Fig.5).

Fig. 5. Eutectic parts produced from Bi3Zn using ASLM [15].

Bismuth, tin and zinc (see parts produced from Bi3Zn from Fig.5) are able to form eutectic alloys with low eutectic melting point. However these materials cannot be used as functional parts due to low material mechanical properties [15]. Further research about ASLM method concerning materials suitable for producing functional parts is being currently made.

3 Experimental part

3.1 Aim of experimental part

Aim of experimental part was to design and manufacture three different support structures carrying a simple rectangular cube (20x9x15 mm). After this, the behavior of the supported component was observed and it was analyzed, how support structure affected the quality of supported region during the process and after removing support detachment.

3.2 Support structure design

Cone-like, X-like and tree-branch-like supports are designed in this study (see Fig. 6, Fig. 7 and Fig. 8). Opened geometry is desired for support structures. Geometry is designed for easy removal of support structures without notching the surface of main part. No free space between supports is left when connecting the supports with each other, supporting totally the supported region. This gives us best possible surface finish and preferably a supported main part without deflections.
Fig. 6. Cone-like support structures.

Fig. 7. X-like support structures.

Fig. 8. Tree-branch like support structures are derived from X-like supports.

Conical geometry is preferred when designing support structures due to preliminary efficient heat transfer from main part to building platform and high part supporting capability. Fig. 6 shows the design of cone-like support structures, where the structure is covering the surface in total area, besides cross-sectional area in the edges, where it is left unsupported for balling effect investigation.
The unsupported area shows high surface roughness. Further design will consider changing the area between the support and main part hollow inside for easier support removal.

For obtaining an opened support structure, designing lighter cone-like structures could lead to part failure due to lower volume of material in bottom geometry. Therefore X-like geometry is designed, where is added volume in bottom. This is made due to high dimensions for main part. X-like support structures (see Fig. 7) are designed with 5 mm of height; it is also in collation with rectangular parallel piped dimensions.

Tree-branch-like supports (see Fig. 8) are designed according to cellular support structures [12] and X-like supports. This support design does not leave unsupported area as for support structures presented in [12]. It is presumably possible to vary the structure in a wide range of relative densities, it is also easy to multiply structure, leaving no free unsupported area between supports. It is easy to connect support structures having different relative densities. This might be required when supporting complex geometries. Raw powder is easily removable due to cellular geometry. Support structure height 5 mm to 15 mm main part is used for design. Cell size of 5x2x2 mm is used.

3.3 Used equipment and materials for experimental part

All experimental tests for this study were carried out at Laboratory of Laser Processing (Lappeenranta University of Technology) with a LAM set-up with IPG 200 W SM CW fiber laser, which is a modified research machine of EOS M series LAM-machine.

Material used is EOS Stainless Steel powder PH1, which is a pre-alloyed martensitic stainless steel in fine powder form, having good corrosion resistance and excellent mechanical properties (presented in Table. 1).

Table 1. Material properties of stainless steel powder EOS PH1 [16].

<table>
<thead>
<tr>
<th></th>
<th>Minimum layer thickness</th>
<th>Hardness</th>
<th>30-35 HRC</th>
</tr>
</thead>
<tbody>
<tr>
<td>Minimum wall thickness</td>
<td>0.3-0.4 mm</td>
<td>Modulus of Elasticity</td>
<td>200 GPa</td>
</tr>
<tr>
<td>Surface roughness after short peening</td>
<td>Rₐ 2.5-4.5 μm</td>
<td>Elongation at break</td>
<td>16-17 ± 4 %</td>
</tr>
<tr>
<td>Surface roughness after polishing</td>
<td>Rₛ up to &lt; 0.5 μm</td>
<td>Ultimate tensile strength (XY- and Z-direction)</td>
<td>1150 ± 50 MPa or 1000 ± 50 MPa</td>
</tr>
</tbody>
</table>

Stainless Steel PH1 provides high yield strength and hardness values. It is mainly used in engineering applications, however also in functional prototypes, small series or individualized products for manufacturing spare parts. Due to corrosion resistance and sterilizability values, it is used also for medical applications [2].

The LAM equipment used in this study was a modified research machine (shown in Fig. 9) representing EOSINT M series consisting of laser equipment, scanner, control movement software and a LAM chamber. Gas proof process chamber was used in this process, shielding gas used is nitrogen.
4 Results and discussion

4.1 Cone-like supports

The part and cone-like support structures are successfully processed (see Fig. 9). The four half-supports near the side edges are also processed without any losses during production. There are no deflections in main part. There is porous surface occurring in the unsupported area (Fig. 9) of the bottom of the main part, having high surface roughness $R_a$ values. This shows the importance of support structure in LAM. The part is not desired to use as functional part. These support structures possess relatively high volume concerning the supported part.

![Fig. 9. a) Cone-like support structures, b) bottom view of cone-shape support structures, c) occurring porous structure in unsupported area between support structures.](image)

4.2 X-like supports

As a result, the part and X-like support structures are successfully processed (presented in Fig. 10). This support is designed to be with minimum volume, it is easily removable. Powder removal is simple from arc spaces. There are no deflections in main part. No balling is occurring neither in main part nor in support structures. For carrying a part having high dimensions and mass values according to the support structure dimensions, these structures are optimal. However, after this manufacturing, it is seen that there is space for further volume reduction.

![Fig. 8. LAM equipment representing EOSINT M series used for actual processing.](image)
Fig. 10. (a) X-like support structures on substrate platform, (b) macrograph of X-shap supports from right view.

4.3 Tree-branch-shape supports

Tree-branch-like support structures carrying a rectangular parallel piped are successfully manufactured. These support structures are with lowest volume and in the same time supporting the part in total supporting area (see macrograph in Fig 11b). The material support removal is the easiest of all supports, between the “tree branches” it easy to remove powder, there is no deflections in main part.

Fig. 11. (a) Tree-branch-like support structures after manufacturing, (b) macrograph from the bottom of tree-branch like support structures.

4.4 Comparison of support structures

The main difference from the three structures is the gradual reduction of support volume. This means reduction of material and also costs. Tree-branch like support structures are easily removable from main part than X-like supports. The support structure function is fulfilled using all support structures, chosen support structure dimension suits the main part dimensions. All structures are simply built with LAM. Raw powder is possible to remove simply from all structures. They are both also perfectly repeatable, leaving no unsupported area.
5 Conclusions and future work

It is concluded that part orientation on the platform must be 45º concerning the recoater to avoid droplets on the part and recoater jams. It has been proven during the experimental set-up that parts orientated under 45º on the platform do not cause recoater movement jams and they do not cause any droplets on the tested parts.

It is also noticed in the experimental set-ups that one-third of the part height (in this study support structure height 5 mm and part height 15 mm) is capable to fulfill the support structure assignment.

It is analysed that tree-branch-like support structure is the optimal solution for low volume support structure. It fulfills support structure functions when supporting the main part having high dimensions. It is suggested to design more “branches” for structure, which gives space reduction and is suitable for manufacturing with LAM, being also in correlation with supported part. This simplifies also the support removal. The unit cell structure is possible to vary with different unit cells and relative densities in a parametric tool, which is also left as future work.

6 Acknowledgement

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TECHNO-ECONOMICAL BENCHMARK STUDY OF LASER ADDITIVE MANUFACTURING OF STAINLESS STEEL PARTS

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Abstract

Laser additive manufacturing (LAM) of stainless steel is a novel technology for manufacturing complex 3D parts from stainless steel via melting steel powder to solid material by laser beam. New materials and high automation level of equipment has made this method more interesting for manufacturing of functional parts. There are only few public studies of techno-economical aspect of LAM in literature and they are several years old. The lack of information is one major hinder for the industrial implementation of the process.

Aim of this study is to define the possibilities and restrictions of LAM process and also to provide a preliminary study of the economical aspect. The study concentrates to evaluate complex geometrical features, such as overhangs, build angle, thin walls etc. The manufacturing costs were studied by using two test pieces whose cost structure was estimated.

All tests in this study were carried out with a prototype research machine equal to commercial EOS M series. The laser in this system is IPG 200W CW fiber laser. The material used in this study was EOS Stainless Steel PH1.

Results show that especially overhangs are critical for making 3D geometries with laser additive manufacturing. Also narrow and high details are challenging to build because those kinds of structures can easily break when the recoater moves. The economic study shows that the main expense is the machine cost. The relative proportion of the material price is very low.

Keywords: Additive manufacturing, laser, techno-economical, benchmarking, stainless steel
1 Introduction

Laser additive manufacturing (LAM) has gained considerable interest in past few years. The biggest topic on this interest is that the quality of the laser additive manufactured parts is on such a high level that the parts can be used in different industrial fields as functional parts. Also, the possibility to create weight and strength optimized parts has raised the interest. LAM is a method in a group sometimes [1] called digital photonic production. In these methods a digital model is processed into a physical work piece using photons – light – as a tool. The cost of LAM manufactured pieces is neither dependent on the complexity nor the lot size of the part as Fig. 1 illustrates.

![Fig. 1](image)

**Fig. 1.** Digital Photonic Production offers benefits both in small series and complex products[1].

LAM can be used effectively in two distinctly different cases. The methods fortes are both in small series of products and also when the complexity of the product is sufficiently high, as seen in Fig. 1. The advantages of laser additive manufacturing are geometrical freedom, mass customization and the possibility to use materials which are difficult to process with other manufacturing methods. It is also important that processes accuracy and ability to manufacture complex geometrical structures are on a good level. Also the process reliability, performance and economical aspects like through put time and cost will play in huge role when using this manufacturing technique in mass production [2,3,4,5]. Of course, this technology as any manufacturing technology has also challenges what comes to the part building. First of all, the designer should know the limitations that this process has. For example that building overhanging features is very difficult. Also creating thin walls and small features is a challenge [4].

There are only few public studies available about the economical aspects of LAM. For example Baumers et al. (2012), Ruffo et al. (2006 and 2007) and J. Allen (2006) have studied the finances of additive manufacturing [6,7,8,9]. The results of studies carried out by industry manufacturing or implementing the technology are seldom freely available and their point of view can be argued to be biased. Therefore there is a demand in the industry for independent economical analysis of LAM manufacturing.

Baumers et al. [6] have studied the costs of LAM. The cost structure shows that the administration and production overheads and labor costs per hour are approximately 41 % of the total indirect machine costs per hour. As the overheads as well as labor costs are very case dependent, it was decided to omit those expenses in this study. Baumers et al. [6] concluded in their study from 2012, that if the payback period for the LAM machine is set at 8 years and the machine operates 5000 hours per year the machine cost is 16.68 € / h. This equals 17.45 € / h, when corrected [10] with retail price index (RPI) to correspond 2013 value. As the economical part of this study is considered as a preliminary approach, and the aim is to give a rough idea of the costs of the test pieces, it was decided to use this value as the machine expense.
2 Aim and purpose of this study

Aim of this study is to determine the possibilities and limitations of LAM process but also to provide a preliminary study of the economical aspect. The study concentrates to evaluate complex geometrical features, such as overhangs, build angle, thin walls etc. Purpose of this study is to understand limitations of LAM process. Aim of the economical analysis was to concentrate on the LAM process induced expenses excluding all other cost factors. The economics of laser additive manufacturing was approached by using a method based on build time estimation. The processing time along with the mass of material consumed, are the main variables in several cost models [6, 7,11]. The total cost of manufacturing can be seen as a sum of the direct raw material costs and the indirect costs of machine operation [6].

3 Experimental procedure

3.1 Lasers used in this study

The laser used in this study was 200 W IPG YLS-200-SM-CW fiber laser. The laser beam is transferred from the laser source to the galvanometric scanner via optical fiber. The used laser produces maximum continuous power of 200 W at a wavelength of 1070 nm and the focal length is 400 mm. The focal point diameter of the laser beam is 70-100 μm.

3.2 LAM machine used in this study

Laser additive manufacturing machine used in this study consists of laser unit, process chamber and process control computer. The LAM machine itself is a prototype machine similar to commercial EOSINT M-series equipment. The building process takes place in the nitrogen filled process chamber. The building chamber is divided into three platforms, where the middle one is the platform where the parts are built. The building chamber is presented in Fig. 2.

3.3 Materials used in this study

Material used in this study was EOS Stainless Steel PH1 powder which is a fine martensitic stainless steel powder and characterized by having good corrosion resistant and excellent mechanical properties. This type of steel material is widely used in engineering applications where high hardness, strength and corrosion resistance is required. Suitable applications can be found for example from medical or aerospace industries [12].

3.4 Benchmark model

The benchmark models used in literature are usually designed to analyze the process accuracy and limitations. Process parameters can be optimized with assist of benchmark studies in order to manufacture parts with higher quality and accuracy [3,13,14]. The benchmark models usually include features such as thin walls or cylinders, overhangs and thin gaps. [3,13,14,15]. Since these kinds of features are difficult to manufacture, it was decided to manufacture test pieces that include these features. Fig. 3 presents 3D models of used test pieces.
3.5 Technical analysis methods

The manufactured pieces were analyzed and evaluated visually. Dimensional analyses were performed to check process accuracy. Before the dimensional analysis the manufactured pieces were lightly sandblasted in order to smooth the surface of the pieces. Microscope used in this study was Nikon SMZ800 with Infinity 1 camera by Infinity Light. Software used for image capture and also for measurements was Infinity Analyze. The measurements were taken multiple times and averaged. Dimensional analysis was also carried out by scanning the manufactured pieces with 3Shape D700 3D scanner. The scanning procedure was carried out in EOS Finland facilities. The 3D scanner scanned the models and created a surface of the models. The scanned surface was then aligned and compared to the original 3D models surface with 3Shape Convince 2010 software.

3.6 Economical analysis methods

The costs were studied in two different cases. First the calculations were made with the assumption that the pieces would be made one by one. Secondly a scenario was calculated in which all six test pieces were built simultaneously. The economics of the laser additive manufacturing was studied according to Baumers et al. [6] with modifications. The approach was to study the costs of LAM machine only; the costs of post processing were not taken into account as they vary according to the user. The total cost can be calculated as equ. (1) shows [6].

\[
C_{\text{build}} = m_{\text{material}} \cdot C_{\text{material}} + T_{\text{build}} \cdot C_{\text{indirect}}
\]  

(1)

where

\( C_{\text{build}} \) cost of build, €

\( m_{\text{material}} \) mass of material, kg

\( C_{\text{material}} \) cost of material, € / kg

\( T_{\text{build}} \) time to build, h

\( C_{\text{indirect}} \) machine costs, € / h.

The direct costs are product of the mass of the sintered piece and the cost of the raw material. The indirect costs are product of the build time of whole platform of parts and an indirect machine cost rate. For an estimate of cost per part \( C_{\text{build}} \) is divided by the number of parts built simultaneously [6]. Since environmental issues and therefore energy consumption are in great interest of the public, it was decided to calculate the electricity consumption separately. For this Baumers et al. [6] propose a more detailed model for the expenses, as equ. (2) illustrates.

\[
C_{\text{build}} = C_{\text{indirect}} \cdot T_{\text{build}} + w \cdot Price_{\text{material}} + E_{\text{build}} \cdot Price_{\text{energy}}
\]  

(2)

where

\( C_{\text{build}} \) cost of build, €

\( C_{\text{indirect}} \) machine costs, € / h.

\( T_{\text{build}} \) time to build, h

\( w \) mass of the piece, kg
$Price_{material}$ cost of material, € / kg

$E_{\text{build}}$ energy consumed during build, J

$Price_{\text{energy}}$ price of the energy, € / J.

Equ. (2) takes into account the energy expenses separated from the machine upkeep and overheads. This method is also suitable for builds containing multiple instances of a certain part. The total cost in this method is divided with the number of pieces built simultaneously in one build. This allocates the costs to the individual pieces. The equation 2 was found to be accurate to less than 10 % by Baumers et al. [6] when they used voxel approximation to calculate the build time. Instead of using accurate volumes of the pieces, they used an approximation by an array of cubes, voxels. The voxel resolution used was 5 mm$^3$.

It was decided to use equation 2 with variations to calculate the expenses of the test pieces in this study. The main difference is that the volume was not approximated, but calculated accurately from the 3D models. Other variables such as cost of the material and energy consumption of the LAM machine were based on findings from literature [6,7,10,11, 12].

The cost calculations were based on estimated build times. The price of energy was obtained from The Energy Market Authority of Finland [16], which is the official source for the price of electricity in Finland.

The final equation used for the calculations for the cost of a build is shown in equ. (3).

$$C_{\text{build}} = C_{\text{machine}} \cdot T_{\text{build}} + w \cdot Price_{\text{material}} + T_{\text{build}} \cdot P_{\text{average}} \cdot Price_{\text{energy}}$$

(3)

where

- $C_{\text{build}}$ cost of build, €
- $C_{\text{machine}}$ machine costs, € / h.
- $T_{\text{build}}$ time to build, h
- $w$ mass of the material used, kg
- $Price_{\text{material}}$ cost of material, € / kg
- $P_{\text{average}}$ average power of the machine, W
- $Price_{\text{energy}}$ price of the energy, € / J.

In equation 3 it is noteworthy to see that the mass of material used $w$ is not equal to mass of the manufactured pieces. It also includes the mass of the support structures.

In the case of a build with all six different test pieces the individual cost per piece was calculated as equ. (4) illustrates.

$$C_{\text{piece}} = \frac{M_{\text{piece}}}{M_{\text{build}}} C_{\text{build}}$$

(4)

where

- $C_{\text{piece}}$ cost of piece, €
- $M_{\text{piece}}$ mass of the piece, kg
- $M_{\text{build}}$ mass of the build, kg
- $C_{\text{build}}$ cost of the build, €.

In equ. (4) the total cost of a build is divided to individual pieces by their relative proportion of mass.
4 Results and discussion

4.1 Technical analysis

In this study the main goals are the evaluation of geometrical features and process accuracy. Especially building small features like holes, walls and gaps are investigated. This study is also meant to give limitations and directions of what kind of parts or features can be built. In Fig. 4 is presented test pieces 1A and 1B top view and bottom view.

As it can be seen from Fig. 4, the direction of recoater movement has very high impact on part build quality. The recoater causes during the recoating such strong forces that the thinnest walls collapse if the part is oriented in 90° angle against the recoater. Otherwise the build quality of these parts does not differ much when visually analyzed from the top side. Bottom side analysis is quite difficult since the parts are sawed off from the building platform and the sawing effects on the small and fragile features.

![Fig. 4. Top a) and bottom b) view of test pieces 1A and 1B with various wall thicknesses. Arrow shows the direction of the recoater movement in both cases a) and b).](image)

As it can be seen from Fig. 4, the building process of the thinnest walls is not successful. It can be also seen that the removing of the parts from building platform may break some of the features. This gives an idea that the minimum achievable dimensions are not only depending on the additive process but also the post processing.

![Fig. 5. Top view, test pieces 2A and 2B. Arrow shows the recoating direction](image)

![Fig. 6. Side views test piece 3A. The arrow shows the building direction.](image)

![Fig. 7. Side views test piece 3B. The arrow shows the building direction.](image)

Test piece 2A and 2B can be seen in Fig. 5. These test pieces include gaps with different widths. Also these pieces were oriented to the building platform in such way that they were 90° and 45° angle against the recoater movement. The third test piece set includes parts with different size holes. The test pieces 3A and 3B are shown in Fig. 6 and Fig. 7.

These pieces were also built in the way that test piece 3A was oriented parallel against the recoating direction and the 3B was oriented in 45° angle against the recoating direction. The
building direction can be seen from the Fig. 6 and Fig. 7 for these pieces. Building of holes is challenging in this direction since many overhangs are created. The marks and cuts shown in test piece 3B B-side came from detaching the piece from the building platform.

### 4.2 Geometrical features and dimensional analysis

Thinnest wall thicknesses of both test pieces are thicker than the thinnest walls in the 3D model. This happens because the laser melts the cross section of the part, and the loose powder around the cross section melts also and attaches to the contours. It seems that in these kinds of features, the build angle does not give big difference in dimensional accuracy. In both test pieces 1A and 1B the thinnest 100 μm wall cannot be built.

The widths of small gaps are analyzed in test pieces 2A and 2B. Gap widths are very close to the nominal dimensions of the 3D model. Gap widths are also slightly bigger when the gaps are parallel to the powder spreading direction. All of the gaps are built but the thinnest ones might not be open all the way.

The third test set included two test pieces where the accuracy of horizontal holes creating overhangs is studied. The test pieces included different size holes from 0.1 mm diameter to 6 mm diameter. The challenge of these test pieces was that they were built in the way that the holes created overhangs, which are difficult to build. It was known that the accuracy of the holes is not very good when they are built in this way. These test pieces were measured from sides A and B. The comparison of average hole diameters between test pieces 3A and 3B are presented in Fig. 8 and Fig. 9.

As it can be observed from Fig. 8 the test piece 3B is more accurate than test piece 3A on A-side of the pieces. The test piece 3B was oriented in the way that it was 45° angle against the recoating direction. It seems that the part orientation has influence on build quality and accuracy in these kinds of features. Since the build parameters are same in both pieces, the only distinctive factor is the build orientation in these pieces. As it can be seen from the Fig. 8 and Fig. 9 the diameter of the smallest hole that can be built is close to 0.5 mm. When building holes in this way that the holes form overhangs it is nearly impossible to build absolutely round holes without support structures. Fig. 9 shows that also the B-sides of the test pieces 3A and 3B differ from the nominal dimensions.

![Graph showing hole diameters comparison](image-url)
In dimensional analysis made with 3Shape D700 3D scanner manufactured models scanned surfaces were compared to the original .STL files of the benchmark pieces. Fig. 10 represents the scan results.

As it can be observed from the Fig. 10, the manufactured models dimensions are very close to the original 3D models dimensions (green color). As it can be seen the test piece 1A and 1B, wall thickness dimensions differs slightly in smallest walls but otherwise the wall thicknesses are very close to the 3D models dimensions as it was noticed also in measurements taken with microscopy software. Similarly the gap widths in test pieces 2A and 2B differs slightly when compared to 3D model.

### 4.3 Economical analysis

The economical analysis in this study was carried out to estimate the expenses of laser additive manufacturing. The methods were found in literature, modified and updated to current price levels. The pieces analyzed in this study were 1A and 3A, see Fig. 3. and Fig. 5. Table. 1 shows cost and cost structure of samples 1A and 3A.

<table>
<thead>
<tr>
<th>Cost</th>
<th>Piece 1A</th>
<th>Piece 3A</th>
</tr>
</thead>
<tbody>
<tr>
<td>Machine cost, €</td>
<td>175.95</td>
<td>108.77</td>
</tr>
<tr>
<td>Material cost, €</td>
<td>0.86</td>
<td>0.43</td>
</tr>
<tr>
<td>Energy cost, €</td>
<td>0.09</td>
<td>0.04</td>
</tr>
<tr>
<td>Total cost, €</td>
<td>176.90</td>
<td>109.25</td>
</tr>
</tbody>
</table>

The analysis for piece 1A shows a total cost of 176.90 €. The costs divide between machine costs, material costs and energy costs. Table. 1. suggests that the main factor in costs is the machine cost. It covers 94 % of all costs. The price of material at this case is 4.8 % and the effect of energy is 1.2 %. The analysis for piece 3A gives similar results. The proportion of material cost
is 3.9 % and the electricity cost 1.2 %. **Fig. 11.** shows that the machine cost is the major factor in the costs of LAM.

It must be remembered that the machine cost includes all consumables, maintenance, shielding gas generation as well as the purchase costs. Still, these costs are unavoidable and independent of the actions of the user. The cost structure of piece 3A is shown in **Fig. 12.** The cost structure of piece 3A differs slightly from 1A. The proportion of material cost is lower. This is due to that the cross-section of 3A is smaller and therefore the proportion of material deposited per layer is smaller. This indicates that the machine time is heavily dependent on the time consumed in recoating procedures.

![Machine cost](image1.png) **Fig. 11.** Cost structure of test piece 1A  ![Machine cost](image2.png) **Fig. 12.** Cost structure of test piece 3A.

The results of comparison between building pieces one by one and all 6 simultaneously are in **Table. 2.**

<table>
<thead>
<tr>
<th>Test piece</th>
<th>Separate build cost</th>
<th>Combined build cost</th>
<th>Savings</th>
<th>Savings</th>
</tr>
</thead>
<tbody>
<tr>
<td>1A</td>
<td>187.07 €</td>
<td>106.97 €</td>
<td>80.10 €</td>
<td>43 %</td>
</tr>
<tr>
<td>3A</td>
<td>114.58 €</td>
<td>65.10 €</td>
<td>49.47 €</td>
<td>43 %</td>
</tr>
</tbody>
</table>

Building the pieces one by one would have taken 45 h 31 min. On comparison, building the pieces in on a single build would take only 25 h 5 min. This yields a time saving of 45 %. This time saving gives savings of similar scale in the cost of pieces, since the cost of material and energy are relatively low. It can easily be seen, that the maximum utilization of the building platform is in key position in cutting the costs. The cost structure is also affected as can be seen in **Fig. 13.**

The cost structure changes rapidly when building multiple pieces at a time. The proportion of machine cost lowers by 45 % when building just 6 pieces simultaneously. The effect of filling the building platform efficiently can be seen clearly as the building time per piece decreases. One major benefit of the LAM method is that several different parts with different sizes and geometries can be built simultaneously. This allows the manufacturer to fill the build space with parts from different orders or customers. This gives a degree of flexibility not seen in other manufacturing method. Many studies [6, 7, 8] indicate that the most cost-effective way to use
additive manufacturing systems is to fill the available build space with as many parts as possible regardless of their shape.

The economic analysis in this study has some noteworthy assumptions. By no means is it not possible to predict the yearly working hours for a machine. As the machine investment cost is by far the biggest factor in the cost of a part, the utilization rate should be very accurate. As machine time is the biggest factor in the price, all measures to reduce the time reduce the cost accordingly. One method would be reducing the material deposited. This can be achieved in many ways in the design phase of a product. As a precise method, LAM needs only thin space for machining. In many cases and especially in many details they are not needed at all.

Excessive material can also be avoided with careful strength calculation. One characteristic method in the case of LAM is replacing solid metal with lattice structures [17, 18]. The designer of LAM pieces must know the manufacturing method thoroughly since its properties and limitations are very different from conventional manufacturing. This approach is known as design for manufacturing and assembly, DFMA. The knowledge about manufacturing methods is imperative to exploit their benefits [17].

5 Conclusions and further studies

Aim and purpose of this study was to look into the dimensional accuracy and object orientation in LAM manufacturing and examine the costs of the pieces produced. The accuracy was found to be sound for most purposes. The orientation of the piece in the build was found to have effect on both realiability and accuracy. The cost of LAM was found to depend heavily on the costs of the machine time. It was concluded that the build orientation effects on accuracy of build features, but it also has an effect of recoating success. The recoating succeed also better when the build parts are not parallel against the recoater blade because in this way the recoater does not hit on straight edges of the parts and jam because of this. It was also concluded that using 3D scanner for analyzing manufactured models is very practical method when the analyzed parts does not include deep gaps, holes or deep grooves.

Machine cost was found to be by far the biggest factor in a LAM manufactured piece leaving the effect of material cost and energy cost to less than 6 % of the total costs. The varying platform filling degree affects significantly to the costs. Building only a single piece at a time was found to be very ineffective. By building only six pieces simultaneously reduced the costs by ca. 45 % compared to building single objects separately. Building several pieces simultaneously also influenced the cost structure. In the case of piece A1 the portion of material cost rose to 8.3 % as the machine and energy cost decreased and the material cost was not influenced. The optimal utilization of the process chamber can be seen as the main variable the user can affect. Taking the building platform into full use is the most important thing the user should concentrate in. Calculations for a case where the building platform would be filled to the maximum would be interesting, as building just six pieces showed a significant decrease in costs.

In future it would be important to examine the price development of LAM machinery in general as it is the biggest influencer in the costs. It also came apparent that the fast developing LAM machinery may not have such a long payback time as assumed in this study. It can be argued that the lifetime of LAM machinery should be assumed to be shorter, not from the point of view of actual lifetime of the machine, but from the pressure that the fast ongoing development of the new machines and software puts on. It was discussed that this pressure is not as high on metallic LAM machines as it is on other additive manufacturing technologies and materials.

6 Acknowledgement

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University of Applied Sciences (Saimia) and Finnish industrial partners. Main goal in the project is to examine fast development methods which create flexible roadmaps for innovations to become successful business stories. Authors thank all participants of the project for their knowledge and input to this article. Authors express as well their gratitude to EOS Finland for their feed-back, assistance and support to be able to execute and publish this study. Authors thank also all the personnel of LUT Laser for their knowledge given to this article.

References


COMPRESSION BEHAVIOR OF LATTICE STRUCTURES IN SOLID SHELLS MANUFACTURED BY SLM

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Abstract

Lattice structures belong to the group of complex structures that can be manufactured in almost any arbitrary shape by Selective Laser Melting (SLM). Particularly for light-weight applications the outstanding mechanical properties of lattice structures such as the high ratio of stiffness / weight are from great interest. Current investigations are undertaken to comprehensively understand the behavior of lattice structures under various loading to explore the design freedom given by SLM. This paper presents first results about the compressive behavior of \fz\textsuperscript{cc}\textsubscript{z}-type lattice structures in solid shells made of 316L-powder. FEM-simulations are carried out to better understand the compressive behavior of lattice structure in solid shells. To determine the influence of side shells and top shells compressive tests are performed on specimens with and without top shells according to DIN 50134. Transition layers are needed to overcome process restrictions to manufacture shells on lattice structures without build errors. A comparison of different types of transition layers situated between lattice structures and top shells is given. Derived from this, design rules to manufacture solid shells on lattice structures with varying cell widths are presented to improve stability and surface quality of top shells.

Keywords: Laser additive manufacturing, Selective Laser Melting (SLM), lattice structures, solid shells, compressive behavior, surface roughness

1 Introduction and motivation

The parts production of additive manufactured products is increasing from year to year having a peak of industrial system unit sales of more than 6000 in 2011 \cite{1}. SLM is a production method to build up metallic or ceramic parts layer-wise directly from 3D-CAD data \cite{2}. The parts are fabricated from metal or ceramic powders with a particle distribution of 20-45 \textmu m. The powder on the substrate is selectively melted by a laser beam according to the sliced CAD model (see Fig. 1). This slicing reduces the complex 3D data into 2D for each layer. After the first layer (approx. 50\textmu m) is deposited and melted, the substrate plate is lowered and additional powder is deposited on the first layer. The laser beam melts the second layer according to the slice data and the layers get metallically bonded. Hence, the final part is built of many single layers. The density of the parts is approximately 100% and this leads to mechanical properties that can even beat conventional manufacturing processes like e.g. die casting \cite{1}. In addition, novel geometries can be generated – no longer limited to design restrictions but only by the imagination of the designer – and can be used to improve performance of conventional product designs by the ability of instant assemblies and the integration of complex internal structures \cite{1}.
Parts need to be detached from the substrate plate (e.g. EDM) after manufacturing and functional surfaces need to be post processed if high surface quality is required. The technology potential of SLM can be described by the two diagrams in Fig. 2. Additive Manufacturing technologies follow a totally different cost relation than conventional manufacturing. The lot-size-one capability of SLM enables well-known new opportunities in business strategy such as mass customization and consumer co-creation [3]. Small batch sizes can be manufactured by SLM at lower fixed costs than using conventional manufacturing. This enables the “Individualisation for free” capability, which can help implementing innovative business models. An additional aspect is the capability of SLM to produce functional-adapted parts in almost any complexity without additional costs. Compared to exponentially increasing costs at increasing product complexity of conventional manufacturing techniques such as machining or casting, costs for SLM-manufactured parts are almost independent from product complexity (Fig. 2).

**Fig. 1. SLM process principle**

![SLM process principle](image)

**Fig. 2. Technology potential of LAM technologies**
No tools, tool-changes, moulds or changing of set-ups are necessary to produce parts with SLM, enabling the cost-efficient production of complex product geometries. This can be summarized to the “Complexity-for-free” capability, for functional improvements of parts geometry by adding additional complexity. High Speed SLM (HP SLM) will increase the “Individualisation for free” and “Complexity for free” capability by reducing piece costs [3].

2 State-of-the-art

The interest for advanced materials in various applications, such as lightweight design, lead to the demand to comprehensively describe the behavior of cellular material. The behavior of cellular material under compressive load can be described by Fig. 3 [4]. Depending on the type of cellular material, two different characteristic curves can be derived to describe the compressive behavior of cellular material. One divides the behavior into stretch- and bending-dominated behavior (see Fig. 3). Stretch-dominated behavior is preferred over bending-dominated behavior due to the better energy absorption capabilities. When designing cellular material these two different behaviors should be considered. Several researchers have investigated the potential of lattice structures manufactured by SLM [5, 6, 7, 8]. The range of different unit cell types varies from cubic structures to topology optimized scaffold structures. Cubic structures are investigated by researchers using a point-like scan strategy to manufacture minimal dimensions [7]. Due to process limitations of the SLM process, the focus of current research is on cubic structures leaving out horizontal struts that cannot be built without support structures (see Fig. 4). The terminology for these types of structures is following the description in crystallography [9]. In [9] power laws are developed to describe the mechanical properties under various loads (e.g. tensile, compression, shear, etc.) for different relative densities. The overall winner of these cubic cell types is f2cc2 giving the best resistance to all different types of loads. The compressive behavior of stand-alone lattice structures of type f2cc2 in a relative density of approx. 10 % is illustrated in Fig. 5. Pictures of deformed structure are given for different states from 0% to 30 % strain to study the failure mechanism of the structure. As shown in Fig. 5 the structure follows an almost perfect stretch-dominated behavior having a high energy absorption capability.
So far only little research is done on lattice structures in solid shells. [10] has developed some general design rules. To manufacture lattice structures in solid shells (hybrid parts) e.g. for medical implants, [9] has given some suggestions. Main aspects in manufacturing these type of structures are related to heat transfer, dimensional accuracy, minimum feature size, mechanical strength and producibility [9]. Stair step effect, residual stresses and the bonding between solid parts and lattice structures have to be considered in the design process. A bonding to a horizontal top plane is quite easy to accomplish when having a structure with small cell width. Connecting a lattice structure to side shells can be much more complicated due to residual stresses leading to manufacturing errors. Warping and balling effect is a big issue when manufacturing structures with certain cell widths [9]. To overcome these issues [9] recommends a minimum diameter of 1.5 mm for cell type fcc and a minimum of 2mm for f2ccz and f2bccz for the attachment of lattice structures to side shells. For reducing manufacturing errors due to balling effect in the connection to top shells, [11] suggests to reduce the cell width constantly to minimize the gap between two struts (see Fig. 6).

3 Design and evaluation of different transition layers

Transition layer design is important to manufacture solid shells on lattice structures of a wide range of cell types and cell widths. The recommended state-of-the-art design is lacking these requirements, working only with certain cell types and cell widths. Additionly the state-of-the-art transition layer is not leading to the maximum achievable surface quality, due to a limited heat transfer and the resulting balling effect.

The objective of the here presented investigation is to develop a suitable transition design, leading to maximum achievable surface quality of top shell. Moreover the mechanical stability of the lattice structure should not be influenced negatively by the top shell. For this reason different FEM-simulations are carried out. Several designs have been developed and rated according to predefined evaluation criterias (see Table 1):

1. **Added volume**: The additional volume added to the lattice structure should be as small as possible, retaining the stability of the overall structure not leading to unnecessary additional weight.
2. **Supported area**: The supported area should be as big as possible to reduce balling effect as much as possible to have an optimum surface quality of the top shell.

3. **Max. Overhang**: The length of unsupported area should be as small as possible to increase the stability of the process and reducing the risk of manufacturing errors.

4. **Manufacturing stability**: The transition layer itself should contain only manufacturable structures with a certain angle to the build envelope to reduce manufacturing errors.

5. **Mechanical stability**: The transition layer should be designed as light as possible, still retaining the stability of the lattice structure, so no weak points are added to the overall structure.

6. **Transferability**: The transition layer design should be compatible to the most important cell types.

### Table 1. Transition layer design comparison

<table>
<thead>
<tr>
<th>Transition layer</th>
<th>Design</th>
<th>Added volume [mm³]</th>
<th>Supporting area [mm²]</th>
<th>Max. Overhang [mm]</th>
<th>Manufacturing stability</th>
<th>Mechanical stability</th>
<th>Transferability</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>![Image 1]</td>
<td>40.41</td>
<td>12.03</td>
<td>2</td>
<td>45°</td>
<td>+</td>
<td>f₂cc₂ + fcc - D-bcc -</td>
</tr>
<tr>
<td>2</td>
<td>![Image 2]</td>
<td>37.72</td>
<td>4.25</td>
<td>7.15</td>
<td>25°</td>
<td>+</td>
<td>f₂cc₂ + fcc 0 D-bcc -</td>
</tr>
<tr>
<td>3</td>
<td>![Image 3]</td>
<td>47.44</td>
<td>14.37</td>
<td>4.76</td>
<td>35°</td>
<td>+</td>
<td>f₂cc₂ + fcc 0 D-bcc -</td>
</tr>
<tr>
<td>4</td>
<td>![Image 4]</td>
<td>57.70</td>
<td>17.51</td>
<td>1.38</td>
<td>35°</td>
<td>+</td>
<td>f₂cc₂ + fcc 0 D-bcc -</td>
</tr>
<tr>
<td>5</td>
<td>![Image 5]</td>
<td>19.65</td>
<td>11.21</td>
<td>4.17</td>
<td>33°</td>
<td>-</td>
<td>f₂cc₂ + fcc 0 D-bcc -</td>
</tr>
<tr>
<td>6</td>
<td>![Image 6]</td>
<td>44.77</td>
<td>38.48</td>
<td>2.40</td>
<td>45°</td>
<td>+</td>
<td>f₂cc₂ + fcc + D-bcc +</td>
</tr>
<tr>
<td>7</td>
<td>![Image 7]</td>
<td>32.66</td>
<td>10.15</td>
<td>2.83</td>
<td>45°</td>
<td>+</td>
<td>f₂cc₂ + fcc - D-bcc -</td>
</tr>
</tbody>
</table>
To determine the optimum transition layer design, a rating of the above mentioned criterias is done. The overall winner of this rating is transition layer design 6, giving a maximum of supported area, while not much volume is added to the overall structure. The transferability to the most important cell types can be achieved, while retaining the mechanical stability of the overall structure.

4 Experimental part

4.1 Aim of experimental part

The aim of the experimental part is to verify the results of FEM-simulation and of the (theoretical) rating of different transition layers. For this reason specimens from 1.5 to 5 mm cell width with a constant strut diameter of 0.5 mm are built. An examination of the specimens is performed to evaluate the quality of the manufactured samples. Lattice structures with state-of-the-art transition layer design are manufactured to compare the surface roughness to the optimized transition layer design 6. To determine the influence of side and top shells on the mechanical properties of the structures specimens with and without shells are build.

4.2 Experimental set-up

All specimens are built on an EOS M270 with a maximum laser power of 195 W with a focal diameter of 80 µm. To ensure dense parts preliminary tests are carried out for finding suitable parameters for struts in a 45° and 90° angle to the building platform. At a laser power of 195 W a scan speed of 900 mm/s leads to a part density higher 99.9 % (see Fig. 7). Contour-Hatch scan strategy is used to manufacture all specimens. To get even surfaces for the clamping in the test machine and to not damage the specimens, the samples are removed from substrate plate by wire cut EDM.

All mechanical testing for this thesis was conducted using a servo hydraulic machine, a Schenck (POZ 0673) with a ±150 mm piston travel and 1000 kN maximum force. The test procedure is according to DIN 50134 [13]. This standard specifies a test method for determining characteristic values for strength and stiffness of cellular materials under compressive load. In the compression test, the samples are centrally placed between the plates of the test fixture and compressed at a constant rate of loading at room temperature. During the compression test, a stress-strain curve is derived to determine the mechanical properties. The compression tests are done at least on three samples of each structure. The DIN 50134 suggests compression speeds from 1.8 to 36 mm/min depending on cell size. All machines are calibrated before usage. The clamping set-up allows specimens to expand laterally.

Fig. 7. Process parameters
5 Results and discussion

5.1 Examination of specimens

All structure sizes are measured by digital caliper to evaluate the accuracy of fabrication. Length and width of structures are manufactured in an average tolerance of 0 to +0.15 mm. The majority of measured data is in the mean of this tolerance. In lattice structures which are covered with both horizontal and vertical shells, deformation can be observed in the intersection of the vertical and horizontal shell. Balling effect can be found on top surfaces. (see Fig. 8)

Side shells shrink approximately 0.2 mm and form a conical shape. This deformation is situated at the middle of the last cells where the transition layer is located. The layer-wise manufacturing process leads to shrinkage of the transition layer and horizontal solid layer causing the attached thin side shell to deform in x- and y-direction during cooling down. The effect can be observed for state-of-the-art transition layer design and optimized transition layer design 6 for certain cell width.

The accuracy of lattice structure struts also are examined by microscope, Zeiss Stereomicroscope, stereo discovery V20 with 10x magnification. Strut diameters, cell width and vertical shell thickness of specimens are measured and it is observed that building tolerance is between -1 to 56 \( \mu \text{m} \) from nominal dimensions (Fig. 9). These deviations need to be considered in the 3D computer model.

Fig. 8. Balling effect and deformation during manufacting of top shell

Fig. 9. Dimensional accuracy of struts (left) and solid shells (right)
Surface roughness of specimens are measured by optical 3D surface measurement machine made by Alicona company with Alicona IFM 3.5.0.1 X64 software. This machine can measure the roughness, shape and orientation of parts. Surface quality of top shells connected with state-of-the-art and optimized transition layer design on a lattice structure with a 4 mm cell width is measured. The surface quality of the state-of-the-art transition layer has a much higher roughness of $R_a=15.073 \mu m$. The optimized transition layer design provides a smooth top surface with $R_a = 3.73 \mu m$ (see Fig. 10).

5.2 Compressive behavior

Two different compressive stress-strain curves are observed when testing fully covered specimens with different cell widths (see Fig. 11 and 12). In these diagrams the dotted lines show the reversible linear-elastic deformation. The curve along the solid line represents the plastic region.

The two specimens show a different behavior in the plastic region. Fig. 11 shows an increase of stress after an initial drop of stress due to structure failure in the beginning of the plastic region. This behavior occurs in structures with cell widths of 3, 4, 5 mm. Fig. 12 shows the behavior of
fully covered lattice structures with cell widths of 1.5 and 2 mm. For these structures no significant drop of stress can be observed in the stress-strain diagram. The different structures can be categorized by their bending-dominated and stretch-dominated behavior. The preferred stretch-dominated behavior occurs for structures with a cell width of 1.5 and 2 mm. This should be considered when designing structures for having a maximum energy absorption capability.

6 Conclusions and future work

Advanced transition layer design can improve the quality of the final part by retaining the light-weight character of cellular material. Surface roughness can be reduced by a factor of 5 compared to the state-of-the-art transition layer design. Moreover an optimized transition layer design helps to overcome building errors that may occur during manufacturing. Deformations may still occur during manufacturing and therefore a further investigation of an optimization of transition layer design is needed.

Solid shells around lattice structures can increase the stiffness and yield strength compared to stand-alone lattice structures as it is already known from conventional sandwich-structures. Smaller cell widths ≤ 2mm show a superior stretch-dominated behavior over larger cell widths (> 3mm).

Future work will be carried out to further improve transition layer design. The objective is to almost fully get rid of an additional transition layer. In this case to overcome the balling effect, the layers underneath the overhang between two struts get premelted with adapted parameters. The layers underneath the solid cover have a lower density than the solid lattice structure and shell, due to the adapted parameters used (see Fig. 13). These parameters guarantee a stable meltpool, so balling effect cannot result into manufacturing errors. The premelted layers are conducting the thermal energy to the struts and reduce thermal stresses in the final part. A great advantage of this new transition layer design would be the low volume of additional material needed. Also solid shells in any orientation (not only horizontal and vertical shells) could be manufactured on lattice structures with this advanced process strategy.

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MEASURING THE STATE-OF-THE-ART IN LASER CUT QUALITY

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Abstract

This paper discusses the strategy appropriate to investigating the state of the art of laser cutting from an industrial point of view. The importance of creating the samples in a high quality industrial environment is emphasised and preliminary results are presented.

Keywords: Laser cutting, Cut quality, Cut edge inclination, Cut edge roughness, Dross, State of the art

1. Introduction

Laser cutting is now a well established industrial process, used for profiling a wide range of materials, from Nickel super alloys to carbon re-enforced polymer composites. The process creates a cut by one of four basic mechanisms or a combination of some of these four;

a. Fusion cutting, or ‘melt shearing’. In this case the laser melts a path through the material and a high pressure gas jet (usually nitrogen) blows the melt out of the cut zone. This is the most common laser cutting method – used for cutting a wide range of metals and thermoplastic polymers.

b. Oxidation cutting. This is commonly used for profiling mild and carbon steels. In this case the assist gas is low pressure oxygen which has two effects on the cutting process; i. It adds energy to the cut zone, allowing higher speeds and maximum thicknesses and ii. it reduces the viscosity of the melt in the cut zone – allowing the use of (cheaper) lower pressures in the assist gas and minimising the creation of dross (residual melt clinging to the underside of the cut edge).
c. Chemical degradation. This is the phenomenon which takes place when cutting materials which do not melt, such as wood-based products and thermoset plastics. In this case the material is burned by the laser and the carbon based ash is blown out of the cut zone by a jet of air.

d. Vaporisation. This is the mechanism by which some polymers are cut – including polymethyl methacrylate (Perspex) and Polyacetal. In this case the material in the cut zone is turned to vapour, which is removed from the cut zone by an air jet.

The original workhorse of industrial laser cutting is the CO₂ laser and the most commonly cut materials are steels. At its best, CO₂ laser cut quality for steels is comparable to a good quality milled edge (see Fig. 1 and Fig. 2).

A relative newcomer to the laser cutting scene is the high brightness fibre or disk laser. These machines can cut thinner section material considerably faster than the more established CO₂ technology, but the cut edge quality is inferior at thicker sections.

In general, it is fair to say that, although most laser cutting cannot be described as precision engineering, the quality and accuracy of the cut edge is more than sufficient for most engineering applications. This point has been proven by the enormous industrial uptake of the process.

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Fig. 1. A typical, high quality cut edge produced when cutting stainless steel with nitrogen assistance (sample thickness: 5mm).

Fig. 2. The high quality cut edge of mild steel profiled with a CO₂ laser and oxygen. The upper sample shown here is 3mm thick, the lower sample is 12.5mm thick.
Although laser cutting has a mature position in industry, there is remarkably little objective information available on the industrial state of the art. This shortfall is being tackled as part of an EU project called HALO (High Power Adaptable Laser Beams for Materials Processing). The HALO project is a European Commission 7th Framework Programme with a number of aims including; an investigation into adaptable beam profiles, modeling of laser cutting and novel cutting processes, and the project also includes the development of a state of the art study.

This paper presents some preliminary results of that study together with a discussion of how such a review should be carried out in order to maximise its usefulness to the industry involved.

2. Optimization of a State of the Art Review of Industrial Laser Cutting

It is important to remember that this is a study of the industrial state of the art and so samples produced in laboratories only have limited relevance. Samples of laser cuts need to be made on very high quality, up to date machines run by skilled professionals (but not scientists). The obvious source of such samples is therefore the applications department of a major laser cutting machine manufacturer. In this case we obtained our samples from Bystronic Switzerland.

The samples are being produced in the most industrially relevant materials (Carbon steel, stainless steel, aluminium, and copper alloys) using high power CO2 and Fibre laser cutting machines over a range of thicknesses.

For each sample the following data are recorded;

a. Material type and thickness
b. Laser type and power
c. Cutting gas type and pressure
d. Nozzle type and standoff
e. Lens focal length and focal position
f. Cutting speed

Each sample was subjected to the following investigation techniques;

a. Optical photography
b. Scanning electron microscopy
c. Optical microscopy (kerf width and HAZ measurement)
d. Optical surface roughness measurement (Ra and Rz)

These investigation techniques were used to put together a photographic and data record of each sample. It is important in a state of the art study of this sort that the information is industrially relevant. It is therefore necessary to identify what relevance any measured property of the cut has on the possible performance of the cut part. For example;

**Cut edge inclination:** Any inclination of the cut edge has an effect on the tolerances which can be applied to laser cut parts. Laser cut edges are not usually exactly perpendicular to the surface of the plate being cut, but the angle of inclination is usually only slightly more or less than 90 degrees as you can see in the Fig. 3.
Fig. 3. A typical kerf cross section – showing that the cut edge is almost, but not quite, perpendicular to the top face of the sheet. (3mm Aluminium cut by CO$_2$ laser).

However, although the cut edge is often slightly inclined, presenting the data as an angle of inclination is unhelpful for two reasons; 1. The cut edge cross section of a cut edge is not a straight line, it is a slight curve. The kerf width might be a maximum at the top, bottom or middle of the cut profile. 2. Industrial engineers are interested in tolerances expressed in microns or millimetres, not angles. For this reason we have taken measurements of the kerf width at the top, middle and bottom of the kerf and presented the results as shown in Fig. 4.

Fig. 4. Kerf width data for aluminum plate cut with a 6 kW CO$_2$ Laser over a range of thicknesses from 1mm to 15mm.
From the raw data given in Fig. 4 we can see that the kerf width is usually largest at the top of the cut and smallest at the bottom, but is occasionally narrowest in the middle. If an industrial part was cut, for example a square with four bolt holes in it, the dimensional tolerances of the part would be determined by the difference between the maximum and minimum kerf widths (see Fig. 5) – and this is the relevant industrial information, as presented in Fig. 6.

Fig. 5. The tolerances of any cut object are determined by the difference between the maximum and minimum kerf width. (a) A kerf cross section; max kerf – min kerf = 300µm – 100µm = 200µm. (b) The dimensions of a typical hole where kerf max – kerf min = 200µm.

If the laser cuts a hole 50mm diameter on the top face of the sheet, its diameter will be 50mm – 200µm (49.8mm) at the bottom face (see Fig. 5b) – so the cutting process tolerances are ‘plus and minus 0.1mm’ in this case. These tolerances for the examples given in Fig 4 are presented in Fig. 6.

Fig. 6. Kerf max – kerf min for the samples presented in Fig. 4 and (lower line) the plus and minus size tolerances related to each kerf.
From an engineering point of view these kerf width tolerances need to be taken into consideration with all the other tolerances of the cutting process when a part is being produced.

**Kerf width:** The kerf width itself is of interest to engineers because it is connected to the limit on the size of detail which can be cut.

**Heat Affected zone:** Heat affected zones can affect the machinability of laser cut parts as well as their tensile and fatigue strengths.

**Dross:** Dross is residual melt which remains attached to the underside of the cut edge after the cutting process is complete. With some laser/materials combinations (eg CO₂ laser cutting steels) dross is insignificant. However dross can be a problem when cutting thicker section steels with Fibre lasers and it generally presents difficulties when cutting thicker section aluminium. A typical thick section cut in aluminium is presented in **Fig. 7.**

![Image of laser cut edge](image)

**Fig. 7.** A typical laser cut edge in thick section (15mm) aluminium alloy cut with a 6kW CO₂ laser. Showing the generally high quality surface with some dross attachment along the lower edge.

Dross can be a problem from a health and safety (sharp edges), aesthetic (it looks untidy), and engineering point of view. As far as engineering is concerned it affects fit-up of parts and prevents parts being stacked on each other without scratching the top surface of the lower part.

**Surface roughness:** The surface roughness of a cut edge can affect fit up, tolerances, wear behavior, tensile strength and fatigue life. Two standard measurements of surface roughness are applied in laser cutting; \( R_a \) – which is the average RMS roughness, and \( R_z \) – the maximum peak to trough measurement over the sampled surface.
In the past surface roughness measurements have been carried out in straight line samples a few millimeters long, but for this present work we are employing optical surface roughness measuring equipment which samples a chosen area rather than a single line. The results are presented as a colour coded ‘contour map’ of the surface like the one shown in **Fig. 8**, which maps approximately one square millimeter from close to the top of the cut edge shown in **Fig. 7**.

![Optical surface roughness measurement](image)

**Fig. 8.** Optical surface roughness measurement. A colour coded contour map of approximately one square millimeter of an area close to the top of the cut edge presented in **Fig. 7**.

Maps like the one shown in **Fig. 8** also give an average $R_a$ roughness measurement for the surface mapped. Several of these maps can be taken over the surface of a thick section cut edge to reveal how the surface roughness changes as we move down the cut edge. The results for a 15mm thick sample – from position 1 (close to the top) to position 5 (close to the bottom) is given in **Fig. 9**.
Fig. 9. *Ra roughness measurements showing how the roughness of the cut surface increases as we move down the cut edge for a 15mm thick sample – from position 1 (close to the top) to position 5 (close to the bottom). Aluminium alloy cut with CO₂ laser.*

**Conclusion**

During the next year or so the HALO project will generate a wide ranging, industrially relevant state of the art study which will be of use to everyone involved in the laser cutting industry.

**Acknowledgements**

This research has been partially carried out as part of the HALO project (314410) which is supported by the European Commission through the Seventh Framework Programme (FP7).

The authors would also particularly like to thank Bystronic, Switzerland for producing the samples, with especial thanks to Dr. Eckard Deichsel and Yves Burkhard.
QUALITY AND PERFORMANCE OF LASER CUTTING WITH A HIGH POWER SM FIBER LASER

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1 Department of Mechanical and Manufacturing Engineering, Aalborg University, Denmark.
2 IPU Technology Development, Denmark.

Abstract

The introduction of high power single mode fiber lasers allows for a beam of high power and a good beam quality factor ($M^2 \leq 1.2$), compared to the multimode fiber lasers often utilised in macro laser metal cutting.

This paper describes fundamental studies of macro laser metal cutting with a single mode fiber laser to study the performance of such lasers in terms of cut quality and process performance. Linear cut experiments have been performed applying a 3 kW single mode fiber laser and varying the following parameters: laser power, cutting speed, focal length, focus position, cutting gas pressure and plate thickness. Parameter studies have been performed in mild steel, stainless steel and aluminium. The cut qualities investigated are: cut width on surface and backside of workpiece, parallelism of top kerf and burr length.

The results are a guideline on how the investigated parameters influence the cut quality and the maximum cutting speed in the investigated parameter space. Furthermore the achieved cutting performance is benchmarked to laser cutting with other types of lasers, CO2-lasers, disc-lasers as well as multimode fiber lasers.

Keywords: cutting quality, high power high brightness, performance benchmark, single mode fiber laser

1 Introduction

Laser cutting of metals is a field in progress, both scientifically and industrially. The field was established in 1967[1] a mere seven years after the construction of the first functional laser, with the first demonstration of laser cutting, using a gas assisted CO2 laser. Since then, CO2 lasers have dominated the field of laser cutting, mainly due to the flexibility of the CO2 laser, allowing for high power levels, easy modulation and scaling the cutting process over a wide parameter range.

In more recent years the CO2 laser has faced increasing competition and a falling share of the laser market, although overall CO2 laser sales are still growing. Generally speaking, the laser materials processing market is doing very well, with an average annual growth rate of nearly 10 % for the last 25 years (albeit with strong annual fluctuations), whereas the general machine tool market grew only about 3 % annually over the same period. While the laser system market fell by 41 % in the wake of the 2008 financial crisis, it recovered swiftly and
experienced record sales in 2011[2]. Accordingly there is a demand for technological improvement in the field.

In the field of laser cutting, the CO₂ laser faces competition from the new short diode pumped solid state lasers with shorter wavelength and excellent beam qualities. The two main competitors are the fiber laser and the disc laser. Both fiber lasers and disc lasers, are more focusable than the CO₂ laser, and can outperform the CO₂ laser in the area of cutting speed for a wide range of materials and thicknesses. One reason for this is that is that the disc and fiber lasers operate at a much lower wavelength, a near-visible 1 μm, compared to the 10.6 μm of the CO₂ laser. At this wavelength, the absorption of the laser beam will be higher for most cutting processes. For the fiber laser, another reason is that the beam can be focussed much more narrowly than the CO₂ laser. In table 1 we summarize the properties of various commercially available lasers for identical powers, focal length and collimated beam diameters. [3], [4], [5] and [6]

Table 1. Summary of the properties for different lasers at identical powers focal length and collimated beam diameter. The benchmark is for commercially available lasers with a max power around 3 kW.

<table>
<thead>
<tr>
<th></th>
<th>CO₂</th>
<th>Nd:YAG (DP)</th>
<th>Disc</th>
<th>MM fiber</th>
<th>SM fiber</th>
</tr>
</thead>
<tbody>
<tr>
<td>Power (kW)</td>
<td>1</td>
<td>1</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Focal length (f_{foc}) (mm)</td>
<td></td>
<td>200</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Collimated beam diameter (D₀) (mm)</td>
<td></td>
<td>12</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Wavelength (λ) (μm)</td>
<td>10.6</td>
<td>1.064</td>
<td>1.030</td>
<td>1.070</td>
<td>1.076</td>
</tr>
<tr>
<td>Beam quality factor (M²)</td>
<td>1.8</td>
<td>35.4</td>
<td>12.2</td>
<td>7.3</td>
<td>1.1</td>
</tr>
<tr>
<td>Beam parameter product (BPP) (mm x mrad)</td>
<td>6</td>
<td>12</td>
<td>4</td>
<td>2.5</td>
<td>0.39</td>
</tr>
<tr>
<td>Focal spot diameter (D_{min}) (μm)</td>
<td>409</td>
<td>800</td>
<td>267</td>
<td>167</td>
<td>26</td>
</tr>
<tr>
<td>Intensity (kW/mm²)</td>
<td>8</td>
<td>2</td>
<td>18</td>
<td>46</td>
<td>1.879</td>
</tr>
</tbody>
</table>

Observing table 1, we see that the single mode (SM) fiber laser has many desirable properties. It has the lowest M² value of the listed laser types and accordingly approaches a Gaussian beam more closely. The SM fiber laser can also be focussed much more narrowly than any of the other lasers, with a much lower beam parameter product and smaller focal spot diameter.

In this paper, the effects of varying the process parameters is discussed, including laser power, cutting speed, focal length, focus position and cutting gas pressure, as well as the material and thickness. Workpiece materials include mild steel (Steel EN DC01), stainless steel (SST EN X4CrNi18-10) and aluminium (Alu 5754).

If we consider the beam diameter $D_z$ and the minimum beam diameter $D_{min}$ in the laser focal spot, the equations are:

$$D_z = D_{min} \sqrt{1 + \left( \frac{4M^2 \lambda z}{\pi D_{min}^2} \right)^2} \quad (1)$$

$$D_{min} = \frac{4M^2 f_{foc}\lambda}{\pi D_0} \quad (2)$$
Where $D_z$ is the beam diameter in mm and $z$ is the distance from focus position in mm. From this we see that the lower the wavelength, focal length and $M^2$ factor (i.e. the better the beam quality as an approximation to a Gaussian beam), the more strongly we can focus the beam. This principle is shown on fig. 1, where we depict the effect of decreasing focal length. The data used to produce the graph is for the 3kW SM laser at AAU; $M^2 = 1.20$, $D_0 = 11.05$ mm and $\lambda = 1.076 \, \mu m$. [6]

![Graph showing the theoretical effect of four focal lengths on the beam diameter.](image)

**Fig.1.** The theoretical effect of four focal lengths on the beam diameter. The shorter the focal length, the smaller the spot size. Numbers in brackets are Rayleigh lengths [mm].

A smaller spot size obviously means higher beam intensity and can lead to a higher cutting rate, though the relation between intensity and cutting rate is not simple. Furthermore, it is not clear that the highest cutting rates occur when the beam is focused on top of the work piece. As such, the focus position is among the parameters which are varied.

## 2 Setup

The laser cell for the cutting experiments was setup at Aalborg University (AAU). The experiments are carried out by positioning a workpiece below the cutting head on a XY-table. Post-cutting experiments were performed by microscope measurements to determine cut widths and cut quality.

### 2.1 Equipment

For the laser cutting experiment is utilised a 3000 W single-mode fiber laser which can be combined with different cutting heads.

The cutting head were mounted, above the XY-table, on a robotic arm. The table is moved by linear electrical motors and has a maximum velocity of 25 m/min. The entire system of XY-table, laser and cutting gas is controlled by an industrial PC. The specifics of this setup are summarized on table 2. The experimental setup is shown in fig. 2 with a modified HighYag BIMO cutting head with 780 mm focal length.
Table 2. Specifications for the laser cutting equipment. Two different cutting heads, each operating at two different focal lengths are used for the experiments. The nozzle and cutting gas remains the same, though the cutting gas pressure is adjustable.

<table>
<thead>
<tr>
<th>Laser</th>
<th>IPG YLS-3000-SM</th>
</tr>
</thead>
<tbody>
<tr>
<td>Emission wavelength, $\lambda$</td>
<td>1076 nm</td>
</tr>
<tr>
<td>$M^2$</td>
<td>1.2</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>HighYag cutting head</th>
<th>Modified HighYag BIMO</th>
</tr>
</thead>
<tbody>
<tr>
<td>Focal lengths, $f_{foe}$</td>
<td>780 and 470 mm</td>
</tr>
<tr>
<td>Collimation length, $f_{col}$</td>
<td>200 mm</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>OptoScand cutting head</th>
<th>Constructed with Optoscan components from the D50 series</th>
</tr>
</thead>
<tbody>
<tr>
<td>Focal lengths, $f_{foe}$</td>
<td>300 and 200 mm</td>
</tr>
<tr>
<td>Collimation length, $f_{col}$</td>
<td>200 mm</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Gas delivery</th>
<th>HighYag BIMO</th>
</tr>
</thead>
<tbody>
<tr>
<td>Nozzle diameter</td>
<td>2.5 mm</td>
</tr>
<tr>
<td>Cutting gas</td>
<td>Nitrogen</td>
</tr>
</tbody>
</table>

| XY-cutting table       | Q-SYS model 0166-01 |

Fig. 2. The cutting head is attached to the robotic arm and positioned above the XY-table on which the workpiece is attached. The entire setup is located in a laser safety cell.

2.3 Quality measurements

In order to determine the cut quality the top and bottom widths of the kerf are measured as shown on fig. 3. This measurement was done primarily using an Olympus BX60 microscope and secondarily a Leitz type 090-124.012 microscope and analysing them with Leica Application Suite from Leica Microsystems.
Both microscopes have adjustable optics allowing for magnification of up to a factor of 50 and 100 for the Olympus and Leitz microscopes, respectively. The magnifications utilised were x5 for broader kerfs and x10 for narrower ones. Length of the burr on the bottom kerf is measured using a vernier caliper.

![Fig.3](image)

**Fig.3.** Measurement of top kerf (1) and bottom kerf (2). The widths of the two sides are measured with a microscope and analysed with Leica Application Suite.

The maximum cutting speed for quality cuts is determined by comparing the top and bottom kerf widths and considering the burr length. Comparable widths of the top and bottom and a low burr length defines a quality cut.

### 3 Experiments

To cover the entire experimental space of workpiece and equipment parameters and process variables would be a daunting task indeed. Accordingly the parameter space was reduced by eliminating some selected combinations. This enables a systematic search of the experimental space to cover the selected parameters and variables influencing the investigated cut qualities without trying all combinations. The values for the parameters and variables utilized in the experiments are given in table 3.

**Table 3. Parameters and variables experimental investigated.**

<table>
<thead>
<tr>
<th>Workpiece parameters</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Materials</td>
<td>Stainless steel (SST EN X4CrNi18-10)</td>
</tr>
<tr>
<td></td>
<td>Steel (Steel EN DC01)</td>
</tr>
<tr>
<td></td>
<td>Aluminium (Alu 5754)</td>
</tr>
<tr>
<td>Thickness</td>
<td>Stainless steel: 1, 2, 5 and 10 mm</td>
</tr>
<tr>
<td></td>
<td>Steel: 1, 2, 5 and 10 mm</td>
</tr>
<tr>
<td></td>
<td>Aluminium: 1, 2 and 4 mm</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Equipment parameters</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Focal length</td>
<td>780, 470, 300 and 200 mm</td>
</tr>
<tr>
<td>Pressure</td>
<td>Variable in the range 0-20 bar</td>
</tr>
<tr>
<td>Focus position</td>
<td>Variable in discrete intervals of 1 mm</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Process variables</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Speed</td>
<td>≤ 25 m/min</td>
</tr>
<tr>
<td>Power</td>
<td>500-3000 watt</td>
</tr>
<tr>
<td>Stand-off</td>
<td>1 (constant) mm</td>
</tr>
</tbody>
</table>
4 Results

Cutting with the single mode fiber laser can provide high cutting speeds. A good cutting quality is not obtained in all cases e.g. all the cuts made in 10 mm plates had burr length above the accepted quality limit, shown in table 4.

Table 4. Good quality specification for different plate thicknesses. The table is not following the ISO standard for classification of thermal cuts [7]. This is because the cut surfaces are not separated for this experimental work, which prevents measurements of the cut surface geometry. The numbers for the difference between kerf top width and kerf bottom width are though in the same range. The other numbers are obtained by costumer needs.

<table>
<thead>
<tr>
<th>Quality</th>
<th>Thickness [mm]</th>
<th>$1 &lt; T \leq 3.15$</th>
<th>$3.15 &lt; T \leq 6.3$</th>
<th>$6.3 &lt; T \leq 10$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Burr length [μm]</td>
<td>$&lt; 100$</td>
<td>$\leq 100$</td>
<td>$\leq 200$</td>
<td></td>
</tr>
<tr>
<td>Difference between kerf top width and kerf bottom width [μm]</td>
<td>$&lt; 200$</td>
<td>$&lt; 300$</td>
<td>$&lt; 500$</td>
<td></td>
</tr>
<tr>
<td>Parallelism of top kerf [μm]</td>
<td>$&lt; 50$</td>
<td>$&lt; 100$</td>
<td>$&lt; 200$</td>
<td></td>
</tr>
</tbody>
</table>

The rest of this paper will only investigate and benchmark cuts that fulfil the specified requirements for good quality cuts.

4.1 Achieved cutting speed with good cut quality

The effect on the cutting quality and maximum cutting speed is presented in this section when varying the focal length, focal position, gas pressure and power.

Focal length

The focal length varied between four levels, across a power interval of 1400 to 3000 W. The maximum cutting speed can be seen in fig. 4 and 5. The cuts plotted on the graphs are within the good quality specifications and it is documented in tables 5 through 8.

Fig. 4. Cutting speed for good quality cuts at 3000W.
Table 5. Parameter settings resulting in good cut quality.

<table>
<thead>
<tr>
<th>Material</th>
<th>Thickness</th>
<th>Pressure</th>
<th>Focal length</th>
<th>Focus position</th>
<th>Burr length</th>
</tr>
</thead>
<tbody>
<tr>
<td>SST</td>
<td>5</td>
<td>5</td>
<td>200</td>
<td>-3</td>
<td>&lt;0.1</td>
</tr>
<tr>
<td>SST</td>
<td>5</td>
<td>5</td>
<td>300</td>
<td>-7</td>
<td>&lt;0.1</td>
</tr>
<tr>
<td>SST</td>
<td>5</td>
<td>5</td>
<td>470</td>
<td>0</td>
<td>0.1</td>
</tr>
<tr>
<td>SST</td>
<td>5</td>
<td>5</td>
<td>780</td>
<td>0</td>
<td>&lt;0.1</td>
</tr>
<tr>
<td>Alu</td>
<td>4</td>
<td>20</td>
<td>300</td>
<td>-5</td>
<td>&lt;0.1</td>
</tr>
<tr>
<td>Alu</td>
<td>4</td>
<td>20</td>
<td>470</td>
<td>-4</td>
<td>0.1</td>
</tr>
<tr>
<td>Alu</td>
<td>4</td>
<td>20</td>
<td>780</td>
<td>0</td>
<td>&lt;0.1</td>
</tr>
</tbody>
</table>

Table 6. Images of the kerf top for different focal lengths for good quality cuts. All images are with the same magnification.

From fig. 4 it can be seen that the maximum speed allowing for quality cuts of 5 mm stainless steel is achieved around a 300 mm focal length where the cut curve is most narrow and appear with the straightest cut, see table 6. On 4 mm aluminium there is almost no relation between cutting speed and focal length. For the 200 mm focal length it was not possible to achieve a good cut quality due to appearance of burr.
**Fig. 5.** Cutting speed for good quality cuts at 1400W.

**Table 7.** Parameter settings resulting in good cut quality.

<table>
<thead>
<tr>
<th>Material</th>
<th>Thickness</th>
<th>Pressure</th>
<th>Focal length</th>
<th>Focus position</th>
<th>Burr length</th>
</tr>
</thead>
<tbody>
<tr>
<td>SST</td>
<td>2</td>
<td>5</td>
<td>200</td>
<td>-1</td>
<td>0</td>
</tr>
<tr>
<td>SST</td>
<td>2</td>
<td>5</td>
<td>300</td>
<td>-2</td>
<td>&lt;0.1</td>
</tr>
<tr>
<td>SST</td>
<td>2</td>
<td>5</td>
<td>470</td>
<td>0</td>
<td>&lt;0.1</td>
</tr>
<tr>
<td>SST</td>
<td>2</td>
<td>5</td>
<td>780</td>
<td>0</td>
<td>&lt;0.1</td>
</tr>
<tr>
<td>Steel</td>
<td>2</td>
<td>5</td>
<td>300</td>
<td>-2</td>
<td>&lt;0.1</td>
</tr>
<tr>
<td>Steel</td>
<td>2</td>
<td>5</td>
<td>470</td>
<td>0</td>
<td>&lt;0.1</td>
</tr>
<tr>
<td>Steel</td>
<td>2</td>
<td>5</td>
<td>780</td>
<td>0</td>
<td>&lt;0.1</td>
</tr>
<tr>
<td>Alu</td>
<td>4</td>
<td>20</td>
<td>470</td>
<td>-4</td>
<td>0.1</td>
</tr>
<tr>
<td>Alu</td>
<td>4</td>
<td>20</td>
<td>780</td>
<td>0</td>
<td>&lt;0.1</td>
</tr>
<tr>
<td>Alu</td>
<td>2</td>
<td>5</td>
<td>300</td>
<td>-2</td>
<td>&lt;0.1</td>
</tr>
</tbody>
</table>
Table 8. Images of the kerf top for different focal lengths for good quality cuts. All images are with the same magnification.

<table>
<thead>
<tr>
<th></th>
<th>200</th>
<th>300</th>
<th>470</th>
<th>780</th>
</tr>
</thead>
<tbody>
<tr>
<td>SST, 2 mm</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Steel, 2 mm</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Alu, 4 mm</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Alu, 2 mm</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

The cuts with 1400W in 2 mm stainless steel and steel show a peak around the 300 mm focal length. For aluminium no effect can be seen because the experiments did not provide data with satisfactory cut quality.

Generally the kerf top width has a tendency to narrow around 300 to 470 mm focal lengths where the highest cutting speeds are achieved.

Focal distance

The maximum cutting speed and the cut quality is investigated around the focus point. In fig. 6 it can be seen that the thicker the material the more the focus position has to be positioned inside or behind the plate both to achieve a good cut quality and increase the speed.
Fig. 6. Lines shows maximum cutting speed achieved and the markers where good quality cuts are achieved. All experiments are made with cutting gas pressure 5 Bar.

Gas pressure

An investigation of the effect of cutting gas pressures is carried out in material of 4 and 5 mm thickness, where the appearance of burr is difficult to avoid during the cutting process. From table 9 it can be seen that the cutting gas pressure has little effect on the maximum cutting speed for quality cuts, when the laser power is set to 1400 W. For a laser power of 3000 W, the maximum speed for quality cuts decreases for increasing cutting gas pressure. The top kerf width is in most cases unaffected and the burr length is in most cases decreased for increasing cutting gas pressure.

Table 9. For the selected experiments all the variables except of those given in the table are kept fixed. The focus position is 0 mm.
Power

For laser cutting it is well documented that the power is proportional to the cutting speed. The cutting experiments show that the best cut quality is achieved around the point of the maximum speed. This relation is shown in fig. 7.

![Fig.7](image_url)

*Fig.7. Influence of the cutting speeds on the quality illustrated for 5 mm stainless steel, power 3000 W, focal length 470 mm, 5 bar cutting gas and focus position at 0 mm. Trend lines are inserted to approximate the best fit.*

4.2 Benchmarking

The theoretical potential of the single mode fiber laser is shown in fig. 8 by benchmarking the cutting performance of other industrial lasers available at the market.

![Fig.8](image_url)

*Fig.8. Benchmark of cutting speed for quality cuts in stainless steel. The reference cuts labelled A to D are obtained through private communication.*
Cut data from the experiments with the SM fiber laser in 2 mm and 5 mm are plotted in the graph. It shows that the cutting speed of the single mode fiber laser is around twice as high as for the other lasers for 5 mm stainless steel while for 2 mm stainless steel the same cutting speed can be performed with half the power for the SM fiber laser than for the any other lasers on the market. The cutting performance of the single mode fiber laser has a huge potential for the industry. Further experiments would be of great interest to determine whether the SM fiber laser continues this high performance over a broader range of plate thicknesses.

5 Conclusion

This work shows that the single mode fiber laser has the potential to increase the cutting speed for good quality cutting by a factor of two compared to the lasers on the market today. This has been demonstrated for 2 and 5 mm stainless steel, but for 10 mm it has not been possible to achieve good cutting results due to formation of burr.

This work did not cover all experimental combinations of stainless steel, steel and aluminium. The achieved cutting speed for good quality cutting in stainless steel is 17.5 m/min in 2 mm thickness with 1400W and 8 m/min in 5 mm thickness with 3000W. For aluminium and mild steel with thickness 2 mm a cutting of 15 m/min is achieved with 1400W.

For the cutting in 2 mm stainless steel an optimum cutting speed for good quality cuts is achieved with a focal length of 300 mm. For increasing plate thickness the optimum cutting speed tends to occur at longer focal lengths. The optimum focus position for maximum cutting speed and good quality is observed to be in the middle and towards the backside of the plate. An increased cutting gas pressure reduces the cutting speed for 3000 W and does not affect cutting speed for 1400W. Furthermore an increased pressure tends to reduce the burr length. Experimentally it is shown that the cutting speed should be maximised for a given power level to achieve the best cut quality.

6 References

MODELLING THE CUTTING GEOMETRY FOR LASER REMOTE FUSION CUTTING OF METALS

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Abstract

The high brilliance fibre and disc lasers that were developed during the last decade provide long range focusing that enables remote laser techniques without assist gas, including remote laser welding and cutting. In combination with industrial robots these lasers offer industry a highly flexible production solution, including quick positioning with scanner optics across a workpiece. The paper presents the state of the art in the field of remote metal cutting with the focus on analytical models and the physical process analyses. Several remote cutting techniques can be distinguished, in particular remote ablation cutting, remote fusion cutting and remote gravity cutting. The three-dimensional cutting process and kerf geometry is concluded to an extensive impact on the melted metal flow and the cutting process performance. A semi-analytical mathematical model has been developed that enables to calculate and analyse the cutting front with respect to absorption, heat conduction, recoil pressure and melt film flow. Beside analysing the remote laser fusion cutting process, the regimes and their transitions from remote fusion cutting to keyhole laser welding, to gas-assisted laser cutting and to laser remote gravity cutting are discussed.

Keywords: remote, laser cutting, fusion cutting, hump, model, front, drop ejection

1 Introduction

Recently remote laser cutting was developed, which differs from the usual gas-assisted laser cutting.[1] The usual way to separate materials by laser beams is through either laser inert gas cutting, Fig. 1(a), or laser oxygen cutting, Fig. 1(b). For both techniques the incident laser beam is absorbed at the cutting front, causing local heating of the workpiece accompanied by melting. An assist gas jet employed concentric to the laser beam ejects the molten material downwards by a shearing mechanism. While during inert gas laser cutting N₂, Ar or another non-reactive gas is used just for the required momentum, during oxygen gas laser cutting in addition the oxygen reacts with the metal, generating additional heat and different material properties. The permanently ejected melt forms a cutting front in form of a melt film flowing downwards where the surface absorbs part of the laser beam. The melt film forms drops at the bottom of the workpiece which either are ejected or attach as dross. A cut kerf forms.
In contrast to these established gas-assisted laser cutting techniques, remote fusion laser cutting, called RFC, see Fig. 1(c), does not use an external gas jet but ejects the melt from recoil (or ablation) pressure from boiling at the cutting front. Another remote laser cutting technique is remote ablation cutting, RAC, where the same recoil pressure mechanism is used in a grooving process, i.e. many scans are repeated to cut deeper into the material and finally through it. A third remote technique is remote gravity cutting, RGC, where the melt drops out due to gravity.

As a major part of this paper the next section surveys the literature of these three remote cutting techniques and modelling approaches for them. The main research objective is to develop an analytical model of remote fusion cutting which so far was not explored very much [2,3]. The model development is in an early stage. Here first concepts, approaches and results are presented.

2 State-of-the-Art

There is usually a high difference between the theoretical speeds for conventional fibre and CO2 laser cutting and what actually are achieved due to limitation of the dynamic behaviour of the cutting machine mechanics. This is especially true for narrow complex paths where cutting reduction up to factor 10 can be expected. Conventional laser cutting methods is also rather inflexible due to dependency of the specific narrow distance between the nozzle and the workpiece [7]. These limitations are a window of opportunity for the remote cutting technology, realizing a higher velocity at the surface of the work piece to be cut. The motion of the focused laser beam is achieved using a flexible scanning system of mirrors that deflects the high quality limited diffraction beam between the laser and work piece surface. The focus spot is capable of vaporizing the desired volume completely or partially without cutting gas and cutting nozzle [4,6,7,8,9].

The references included in this study and their respective area of research within remote cutting is summarized in table 1. The table also gives a brief overview of the magnitude of research activities in different areas.
2.1 Remote Ablation Cutting (RAC)

In the remote ablation cutting process, also called vapour pressure cutting [10], material is removed by vaporizing and melting. A single mode fibre or disc laser in cw mode with a high quality beam is used [2,3,6], related to beam parameter product (BPP) as low as 0.5 mm.mrad or better, with high power of several kilowatts. This provides a very good focusability and a high intensity in the range of $10^7-10^{10}$ W/cm² [5,12] needed to carry out the vaporization. To achieve remote cutting in steel the scanning velocity of the spot is between 360-720 m/min. The cutting depth per exposure is limited to 50-100 µm and several exposures/scans have to be carried out for higher thicknesses [3]. An equivalent velocity $v_p$ can be calculated that is equal to scanning velocity divided by the number of scan. This multi-exposures also reduce the quality of the cut [14].

The molten material is removed from the top surface of the kerf by the vapour pressure generated from the evaporated material that expands in gas state condition. When the high intensity beam evaporates the material partly, the built up strong recoil pressure squeezes and accelerates radially the fused material from the centre to the kerf walls. The melt flows upwards towards the cutting kerf aperture and blows out or ejects. A large amount of droplets leaves the workpiece as spatter with the velocity of 20-40 m/s, see Fig. 2. Most references state that a focused beam must be capable of vaporizing the volume to be removed completely or partly. They also proved that vaporizing of material is only a fraction of the removed material; the rest is assumed to be melted and ejected by action of recoil pressure [4,5]. The necessary increase in the enthalpy per unit volume is between the need of energy for fusion and vaporization cutting (sevenfold amount of specific energy in comparison to conventional fusion one) [5]. Therefore the disadvantage of remote cutting is mainly the higher energy requirement due to nonexistence of the gas flow [7]. Another disadvantage is the formation of burr on the laser access side of the cutting edge [3]. The kerf width is very narrow compared to traditional laser cut kerfs [12].
2.2 Modelling of RAC

Musiol [2,9] presented a RAC model that explains the melt ejection mechanism driven by recoil pressure. The model includes the absorption of the laser beam at the cutting front surface, the acceleration of the melt along the pressure gradient and the material ejection. The front wall geometry used in the model, based on high speed imaging results, shows that the shape of the absorption front remains as a step wall and is a slightly forward inclined cubic function, see Fig. 3. A Gaussian distribution was used which implies that the highest intensity and therefore the highest irradiated power is located at the root of the front wall. It is assumed that the highest temperature, consequently the highest vaporization rate and the highest pressure on the melt due to the recoil pressure of the exiting gas occurs there. Besides the minor ejection in cutting direction (forward), a major amount of the material ascends along the side wall to the edge of the kerf and leaves the part as spatters in sideward and reverse direction.

Otto [13] created a framework of modules within open FOAM software package that involving different physical phenomena coupled together, which simulate thermal and fluid dynamic effects for gas-free remote cutting.

2.3 Remote Fusion Cutting (RFC)

This process also called vapour pressure fusion cutting [14] or melting pressure cutting [10]. The RFC process was developed by Trumpf AG and it is applicable for sheet thickness up to 4 mm. It can be used in the same equipment as for remote welding. This is an advantage for RFC. In RFC the material is vaporized, which creates a recoil pressure on the molten material and ejects the material from processing zone in the opposite direction of the laser beam irradiation. The force needed for this ejection is created inside the kerf, without any assistant cutting gas, and the kerf is formed by one scan. The resulting intensity on the work piece is
around $10^6$ W/cm$^2$, which is within the scale of keyhole welding. RFC required lower processing speed around 4-12 m/min depending on thickness and applied power, and spot size of about 600μm [3,11,14].

There are approaches to describe the melt expulsion with the process models from remote keyhole welding [3,13]. A combination of low scanning speed with high laser power will lead to a high amount of energy to be deposed in the keyhole. This energy is absorbed at the front keyhole wall in the weld direction, causing melting and some evaporation of the material. In the process the keyhole opens up at the bottom, forms a continuous gas capillary and clears the way for the material to leave the processing zone. The vapour generates a recoil pressure on the melt layer which is resulting in a stream away from the irradiated zone. Due to the melt flow around the keyhole and the instability in the keyhole front wall, that is typical for deep penetration welding, humps will be created in the keyhole front wall when the propagation velocity of the melt-layer exceeds the laser spot translation speed. The keyhole melt runs away from the laser beam and produces humps that move down the keyhole wall [3]. At certain machining parameters the momentum of these humps is high enough to exceed the forces due to surface tension at the outlet of the keyhole. Thus droplets can detach from the workpiece at the bottom end [13]. Due to humps that occur during the process, parts of the cutting front are shadowed from the direct exposure to the laser beam which generates varying vapour pressure along the cutting kerf [11,16].

Schäfer [14] comments that vapour pressure fusion cutting uses only two material flows that are developed in the processing zone around the keyhole at deep penetration welding. The first one is horizontal, radial circulation of vapour capillaries as a result of the pressure differences in front of and behind the capillaries resulting from the feeding process. The second is a vertical down-flow at the irradiated surface in front of the vapour capillary. It develops from the repulsive forces acting in the beam direction that the vaporizing metal applied on the molten material, see Fig.4(a).

Schäfer [14] added concerning the mechanism of ejection that the adopted irradiated surface is an inclined plane whose surface is evenly vaporized though it in fact forms a wave-shaped surface structure, see Fig. 4(b). Due to this structure the beam interacts with the surface in different angles of incidence and that affects the absorbed power and results in varying levels of vaporization. The repulsive force distorts the conceived ideal surface into a type of wave shape stairs. This effect depends on the type of laser used. For solid-state lasers on the wave’s top side at an irradiated angle near zero degrees exceeds by three times the absorbed intensity on the wave flank at an irradiated angle between 70-80 degrees. The pressure differences on the irradiated surface resulting from this trigger a kind of moving humps that effect the strength of the vertical current flow.

(a)

(b)

(c)

Fig.4: Remote fusion cutting (RFC), (a) Circulation of the vapour capillaries (current 1 and current 2) that are essential for vapour pressure fusion cutting[14], (b),(c) comparison of ideal and real irradiated surface [14].
2.4 Modelling of RFC

Otto [13] created a framework of modules within open FOAM that involves different physical phenomena coupled together which simulate thermal and fluid dynamic effects for vapour pressure fusion cutting, see Fig. 5.

![Diagram of vapour pressure fusion cutting](image)

**Fig. 5:** Calculated vapour pressure fusion cutting; laser power 3kW, λ 1.06μm, beam radius 300μm, feed rate 4.2 m/min, material stainless steel[13], time (a) 0.5ms, (b) 3.8ms.

2.5 Remote Gravity Cutting (RGC)

This technique is based on powerful CO₂ or fibre lasers for performing dismantling in contaminating equipment or area where those surfaces are difficult or impossible to reach by the operator or robot [8,17,18]. Since the cutting is done without gas jet and with radiation intensities that are insufficient for evaporation of the surface, the outflow of the molten material is driven by gravity, thermocapillary forces and oxidation reactions. Some of its unique characters are: a cut width that substantially exceeds the size of the focusing spot by factor of 2-10; an oscillating process (one can see that the interaction zone is heated, the melt flows out and then cools down and the process is repeated); and that the loss of the specimen mass is small compared to the mass of the remelted material [18].

2.6 Modelling of RGC

Antonova [17] suggested a two-dimensional thermal model for RGC based on the assumption of instantaneous removal of melt when the melt zone size reaches its critical value. The calculated stable cutting region is dependent on the cutting speed, laser power and thickness of specimens. The shape of the melt zone will be determined by the solution of the equation of transfer of heat supplied from the laser. The cut width noticeably exceeds the thickness of the plates.

2.7 Transitions between different remote regimes and remote welding

A transition from remote laser cutting to remote laser welding can be obtained by mainly changing the processing speed or the beam power. Lütke [8] investigated two transitions by several sets of experiments, from a process similar to RGC, to small transition of welding, and from welding to RFC by increasing the speed, see Fig. 6(a). Both cut shapes are round due to incomplete melt ejection that leads to re-solidification of remaining melt within the process zone. Musiol [2] demonstrated that when the welding velocity is increased beyond 30 m/min, the regime gradually changes to remote ablation cutting. The keyhole rear wall becomes unstable and disappears irregularly, leading to an alternation between ablated and welded areas along the laser path until a permanent kerf is formed.
Schober [11] studied the cutting front during remote fusion cutting experimentally, using a high speed camera. He found that the cutting front angle strongly depends on the cutting velocity and laser power. The thickness influences the minimum and maximum values. The explanation is according to Schober that the laser beam exposure of the cutting front decreases with increasing thickness. He pointed out two possible mechanisms; First when the velocity of the laser is reduced, the energy input per unit length rises and thus more material will be molten. Therefore the material around the laser spot will increase. The vapour pressure in the process area will not change significantly and a constant amount of material will be ejected. If the rise of molten material around the spot reaches a critical value corresponding to lower speed, the process will stop because the vapour pressure cannot drive all molten material out. Second when the cutting velocity increased, less material will melt per unit length. The narrow melt pool will induce a velocity around the laser spot. The vapour pressure will remain unchanged and the velocity reaches a point where more material is delivered to the back of the laser spot than can be ejected. The process will end and a weld seam will remain [11], see Fig. 6(b), for a stable cutting area (2-b), and to increase or decrease the cutting speed results in an unstable process (welding, corresponding to 1-b and 3-b area).

![Fig.6: Transition between remote laser cutting regimes and remote welding](image)

(a) Transition between remote laser cutting regimes and remote welding, (a) typical process regimes from RGC to small transition welding and RFC from “A” to “C” as feed rate rises[8]; (b) process region for remote fusion cutting of DC04 [11].

3 Approach for modelling remote fusion cutting

For Remote Fusion Cutting, RFC, the development of a mathematical model has been initiated. Aim of the model is to describe, in a simplified manner, the main mechanisms of the cutting front in RFC, including the transitions across limiting regimes. The quasi-steady state process will be basically described by a simplified model of the melt film.

Different fundamental regimes can be distinguished with respect to the melt film flow, as illustrated in Fig. 7 by a horizontal cross section of the melt film, bounded by the absorbing front (at boiling temperature) and by the solid-liquid interface (at melting temperature). As a regular RFC-process, mainly vertical flow of the melt film can be expected, see Fig. 7(a), driving the melt downwards for ejection. The melt film becomes thinner to the sides. For certain conditions, e.g. for high cutting speed, a lateral flow component of the melt film becomes significant, see Fig. 7(b). If this lateral flow component is of the order of magnitude of the vertical flow component, part of the melt will flow around the laser beam backwards and resolidify as a recast layer at the cut surface, see Fig. 7(c). The other extreme to vertical flow is completely horizontal flow, which is one way of carrying out deep penetration laser
welding, where the melt recombines behind the laser beam (and then keyhole) and resolidifies to a weld. So far, a smooth processing front was assumed for Figs. 7(a)-(c).

High speed imaging gave evidence that at least under certain conditions the keyhole in laser welding, and similarly the front in laser cutting, forms waves or humps, see Figs. 7(d)-(f), with otherwise three regimes comparable to Figs. 7(a)-(c). A wavy processing front differs from a smooth front with respect to its absorption and its momentum transfer. The absorption of wavy (inclined) processing fronts was comprehensively studied [16,23]. A smooth front keeps the (direct, so far disregarding multiple reflections) absorptivity rather homogeneous across the surface, mainly governed by the operating angle in the Fresnel-absorption curve. In contrast, waves strongly modulate the absorptivity and even easily cause shadow domains. The strong, more and more stochastic spectrum of angles and in turn of absorptivity, according to Fresnel, contributing tends to an angle-averaged absorptivity. Moreover, a wavy surface that was generated by local strong beam absorption and boiling will cause a recoil pressure that generates a preferred vertical melt film flow component, while the smooth surface basically should equally “squeeze” the melt film into the lateral and both vertical directions, i.e. also upwards, in contradiction to the experience of drop flow downwards.

As a first step the mathematical model has been developed to calculate the (x-location of the) cutting front, $x_{cf}(z)$, and the melting front, $x_{m}(z)$, in the centreline of the cut, by vertical discretization $z_i$ in z-direction, see Fig. 8(a). So far only a smooth surface at boiling temperature is assumed, the broken line in Fig. 8(b), while waves or humps (the solid curve) will be introduced later.

**Fig. 7:** Cutting front melt film section viewed from the top – different regimes: (a) smooth cutting front, flowing downwards, (b) partial sideward flow, (c) strong resolidifying side flow, (d) wavy cutting front, flowing downwards, (e) partial side flow, (f) strong side flow

**Fig. 8:** Cutting front viewed from the side: (a) model for the melt film, discretized into depth $z$ (L...laser beam, M...melt film, v...cutting speed), (b) detailed view of smooth (broken line) vs. wavy cutting front (solid line)
Vertically downwards from the top surface an energy balance is solved, between the incident power density of the laser beam, Eqs. (1)-(5), and the heat required to achieve boiling temperature, Eq. (6). The energy balance calculates the angle of inclination $\theta_w$ of a vertical element $z_i$, see Eq. (7). This inherently considers the angle-dependent Fresnel absorption $a(\theta_w)$. Multiple reflections are not yet taken into account.

\[
I_0 = \frac{2P_L}{\pi r_{f0}^2} \\
r_{f0} = \frac{2\lambda F M^2}{\pi} \\
Z_s = \pm 2r_{f0}F \\
r_f(z) = r_{f0}[1 + \left(\frac{z - z_0}{Z_s}\right)^2]^{1/2} \\
I(r, \varphi, z) = I(x, z) = I_0(r_f(z)\ exp(-\frac{2r^2}{r_f^2})) \\
q'v(r, \varphi) = (T_v - T_a)\lambda v\ (\cos \varphi + \frac{K_q(P e' r)}{K_q(P e' r)}) \\
\tan(\theta_w) = \frac{q(x)}{i_0(x, z)\alpha} = f(x, z) \\
\]

From the angle per vertical element $z_i$ the boiling front $x_{bf}(z_i)$ is calculated. As a second step the melt film thickness is calculated, assuming vertical flow downwards, as in Fig.8(a), by a momentum and mass balance, Eqs.(8),(9). The driving force is the recoil pressure from boiling, $p_r$, so far assumed constant across the front, see Eq.(10),(11) [21,22]. The thickness of the melt film (in the central plane) is $S=x_m-x_{bf}$.

\[
\frac{\rho}{2}v_z^2 + \frac{\mu}{\rho}v_z + \left(\frac{r_f}{r_f} - P_r\right) = 0 \\
S = \left(\frac{2\rho r_f v_z d_\varphi + 2v_z d_\varphi}{2v_z}\right) \\
p_r = \frac{A P_r(T_s)}{AB_0(T_s)} = AB_0 T_s^{-1/2} \exp\left(-\frac{U}{T_s}\right) \\
U = M_\alpha L_k N_\alpha K_b \\
\]

where: numerical coefficient $A=0.55$ depending on the environmental process, vaporization constant $B_0=3.9*10^{12}$ [kg m$^{-1}$s$^{-2}$], $T_s$: surface temperature in [K], atomic mass for iron $M_a=55.845$ [g/mol], latent heat of vaporization $L_v=6088$ [J/g], Avogadro number $N_a=6.022*10^{23}$ [mol$^{-1}$], Boltzmann constant $K_b=1.387*10^{-23}$ [J/K], $\rho$: density= [kg m$^{-3}$], $\sigma$: surface tension coefficient [N m$^{-1}$], $v_z$ average downward velocity [m s$^{-1}$], $\lambda$: laser wavelength [\mu m], $P_L$: applied laser beam power [W], $\alpha$: circularly polarized light absorbptivity [-], $\beta$ is the angle of incidence (defined to be zero at normal incidence), $F$: the focusing number of the focusing optics, $r_0$: focal radius, $r_f(z)$: beam radius, $I_0$: peak power density, $S$: melt film thickness, $M^2$: beam quality, $p_r$: recoil pressure, $\mu$: dynamic viscosity

For first calculations a Gaussian beam profile was used, Eqs. (1)-(5), which will be extended to the profile for focused fibre-guided laser beams. A wavelength of 1070 nm was used, for 6 mm thick low carbon steel.
4 First results from modelling

First cutting front geometries were gained from the model, including the vaporization front location \( r_{fv}(z) \) and the solid-liquid interface location \( r_{fm}(z) \) along the depth \( z \), in the centreline of the process, as shown in Fig. 9(a),(c) for a cutting speed \( v \) of 5 m/min and 15 m/min, respectively. The calculations were carried out for 6 mm thick low carbon steel. A laser power of 8 kW (cw) was applied, wavelength 1070 nm, focusing to a spot diameter of 137 µm and a Rayleigh length of 30.76 mm. For sake of simplicity so far a Gaussian beam was applied, which will be changed to real focused fibre-guided beam, i.e. a top-hat like profile in the focus, changing to a Gaussian-like profile in the far field. The focal plane position is at the top surface, \( z_0=0 \).

![Graphs](image)

**Fig. 9:** Results calculated from a simple model: (a) cutting front and melt film as a function of depth \( r_{fv}(z) \), \( r_{fm}(z) \), for \( v=5 \) m/min, (b) average melt film velocity as a function of depth, \( v(z) \), for \( v=5 \) m/min, (c) cutting front and melt film for 15 m/min, (d) melt film velocity for 15 m/min

The calculated (average) downward melt film velocity along depth, \( v(z) \), is shown in Fig. 9(b),(d), for the two cutting speeds studied. For this simple model the melt film starts at high velocity (which is uncertain and needs improved modelling) and reduces into depth while the melt film becomes thicker, maintaining the mass balance. The mass flow increases downwards, hence the melt film grows stronger than the velocity decays.
Summary

- Three main techniques of remote laser cutting are distinguished: remote fusion cutting, remote ablation cutting, remote gravity cutting.
- A survey table on the state-of-the-art in remote laser cutting was established.
- Remote cutting is not much explored yet; in particular, mathematical models were mainly developed and applied for RAC and RGC, less for RFC.
- For RFC, a model approach has been initiated in this study, based on calculation of the cutting front including its melt film and comparing smooth to wavy front topography.
- The transition regimes limiting RFC are under investigation, particularly the transition to keyhole laser welding and to possible spatter ejection.
- First preliminary results by a simple model version on the cutting front and melt film indicate some trends; a more sophisticated model is under development.

Acknowledgements

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References

LASER WELDING WITH HIGH POWER LASER: THE EFFECT OF JOINT CONFIGURATION

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Abstract

The use of high power lasers with wavelength around 1000 nm is growing fast. These lasers are compensating the conventional CO₂ lasers in various new applications due to the fact that beam transfer with optical fiber provides various beneficial features to actual welding process and machinery used. These lasers are still not through studied in respect of their behaviour in various cases of different joint configurations possible for keyhole welding. Occasionally the power density can be too high for welding of specific case. This study concentrates on behaviour of keyhole process and effect of this to weld quality and properties in various joints and penetrations. The tests were carried out with stainless steels and S355 structural steel in case of bead on plate, butt joint and fillet joint welding. The quality of weld in various cases changed according to joint preparation and welding parameters. It was possible to reach acceptable quality in all cases. The quality and process performance is depending on beam quality, joint configuration and welding parameters. In case of stainless steel it was seen that the keyhole behaviour is different in case of bead on plate welding compared with welding of a butt joint. The experiments show clearly that these lasers have great potential in welding and they will gain larger role in welding of e.g. thick section steel structures, but the processes are not usually ready for industrial use, but the systems and process require further development.

Keywords: Laser welding, keyhole welding, joint preparation, groove preparation, joint type, fillet weld
1 Introduction

Recent research show that with high power fiber lasers the edge surface roughness has a large effect on absorption at high power levels ($P \geq 5$ kW). Edge surface roughness strongly modulates the local absorptivity during the laser beam welding process, this tendency was first recorded by Arata and Myamoto in year 1972 [1]. Later experiments with 4 mm thickness stainless steel with different edge surface roughness performed by Covelli et al. [2] with 2.5 kW CO$_2$ laser have shown that properties of the welds are not affected by the surface roughness. Bergström et al. [3] recorded a trend of increasing absorption for increasing roughness above Ra 1.5 $\mu$m for stainless steels and above 6 $\mu$m for mild steels by reflectance measurements. Sokolov et al. [4] [5] observed a correlation between edge surface roughness and absorption in welding of structural steels in butt joint configuration with penetration depth and measured the absorbed energy.

The joint preparation influences the geometry of the keyhole, which consequently affects the geometry of the weld. The welding parameters change the behavior of the surface melt which affects to the bead reinforcement capability. In some cases the bead reinforcement can be sufficient but with other parameters underfilled. Welding speed is the main affecting parameter [8]. The effect of air gap in case of welding of fillet T-joint and laser-MAG hybrid welding has been shown to be significant as well, showing considerable increase in welding speed or penetration, which is preferred. [11, 12]

Joint edge preparation is an important part in the laser welding, and with properly made joint edges the efficiency of the process can be improved. The tests were carried out with stainless steels and S355 structural steel in case of bead on plate, butt joint and fillet joint welding.

2 Aim and purpose of this study

Aim of this study is to determine the possibilities and limitations of laser welding with high power in case of various joint preparations. The study concentrates on evaluation of the effect of joint type and groove preparation on the weldability with high power modern laser with wavelength suitable to fiber optic transfer.

3 Experimental procedure

In this paper four different studies are presented. The influence of edge preparation on process efficiency and characteristics of the joint has been studied in butt joint, bead-on-plate and fillet joint configurations. Modern high brightness lasers were used as laser power sources. Experimental arrangements used in these studies are presented in Table 1.
Table 1. Experimental set-ups. BJ - butt joint; BOP - bead on plate; FJ - fillet joint, \( P_L \) – laser power, \( v_w \) – welding speed, fpp – focal point position.

<table>
<thead>
<tr>
<th>No</th>
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<th>Joint type</th>
<th>Laser</th>
<th>Power [kW]</th>
<th>Variables</th>
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<td>15</td>
<td>BJ</td>
<td>IPG YLS 10000</td>
<td>10</td>
<td>Edge Roughness</td>
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<tr>
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<td>BJ</td>
<td>IPG YLS 15000</td>
<td>15</td>
<td>Edge Roughness, air gap</td>
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<tr>
<td>3</td>
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<td>IPG YLS 10000</td>
<td>10</td>
<td>Edge Roughness, air gap</td>
</tr>
<tr>
<td>4</td>
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<td>8</td>
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<td>5 kW disc laser</td>
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<td>Fpp, ( v_w )</td>
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<tr>
<td>5</td>
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<td>BJ</td>
<td>TRUMPF TruDisc16002</td>
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<td>( P_L ), ( v_w ), fpp, edge preparation, transfer fiber Ø</td>
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<td>10</td>
<td>FJ</td>
<td>IPG YLS 10000</td>
<td>8, 9.5</td>
<td>( P_L ), ( v_w ), fpp, offset from the edge, beam angle</td>
</tr>
</tbody>
</table>

Power means laser power in kW, adjusted from the laser control unit. Edge roughness is measured from the midsection of each joint edge with typical roughness measurement tools. The roughness is given, like typically in mechanical engineering, as \( R_a [\mu m] \) value. Focal point position, fpp, means the location of focal point relative to workpiece top surface. The positions below it are marked with minus and positions above with plus. E.g. fpp -6 mm means that focal point is located 6 mm below workpiece top surface.

4 Results and discussion

4.1 Effect of surface roughness on welding performance

The effect of surface roughness on penetration depth was tested at laser power > 10kW for low alloyed steel. Fig. 1 shows the results of the experimental carried out in butt joint configuration. A significant dependency can be observed in depth of penetration as a function of the surface roughness showing an optimum roughness when penetration depth is taken into account.

Fig. 1. Penetration depth at different edge surface roughnesses and power levels. Setup 1, setup 2.
The hypothesis behind this experiment series was that edge surface roughness has a critical effect on the welding efficiency only at the very beginning of the process, when the keyhole is initiated, while after the stabilization of the keyhole it does not affect on the optical or absorption properties of the edge surface. [6] Since the weld length used in most of the experiments was quite short (200-300 mm), this idea may look like of low significance, but for the industrial applications of several meters long welds, these data can lead to valuable modifications in the welding process. This hypothesis was tested with two variant setups: butt joint with a constant edge surface roughness of Ra 6.3 μm and butt joint with 50 mm of edge surface machined to Ra 6.3 μm at the beginning of the sample and the rest 300 mm to Ra 3.2 μm. The results of the experiments are shown in Fig. 2. Analysis of variance shows that full machining gives more stable process and also a higher repeatability of the results.

These results suggest that edge surface should be constant along the whole length of the joint for welding process to be stable, which is controversial to the initial hypothesis.

4.2 Effect of groove preparation on welding

In this set of experiments the main aim was to find out characteristics of different types of edge preparation methods thus their effect on weld quality. Methods used were machining (milling), machining with grid blasting, abrasive waterjet cutting (AWJC) and laser oxygen cutting (LOC). These methods were evaluated in different plate thicknesses in I-groove butt joint configuration. From LOC cut samples the oxidized surface was removed by grid blasting. All edge preparations were made with industrial machines representing standard industrial quality. The used groove preparations are shown in Fig. 3.

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These results suggest that edge surface should be constant along the whole length of the joint for welding process to be stable, which is controversial to the initial hypothesis.
4.2.1 Abrasive waterjet cutting

Abrasive waterjet cutting (AWJC) as an edge preparation method was tested in two different ways. In case of AWJC and LOC cutting the subcontractor used most efficient cutting process, which does not necessarily represent the best possible cut kerf quality for laser welding. Since the typical AWJC cut edge is not perpendicular against the top surface of the workpiece, see part A in Fig. 4, there is two optional ways of setting the workpiece edges. If both edges are in the same position as cut in cutting machine, top-to-top set up, a small V-groove is formed to joint (having top width of 0.5 – 1.0 mm). If either of the workpieces was turned upside down, so called top-to-bottom configuration, this V-groove and air gap in the top side were eliminated. Top-to-bottom configuration is not much better than with top-to-top, since joint is tilted from being perpendicular to workpiece and gap does not align with the used direction of the laser beam, see part C in the Fig. 4. This tilted I-groove has equal air gap throughout the whole thickness, but with certain laser power the penetration is shallower, since the laser beam hits the base material, instead of groove edge, see Fig. 4 part C where weld locates next to the joint in the base material.

Fig. 4. Edges prepared with abrasive waterjet cutting. Setup 6. A) joint groove used in B, abrasive waterjet cut, top-to-top. Tack weld at the root was only at the end of the weld. B) $P_L$ 15 kW, $v_w$ 2.75 m/min, fpp -7 mm. C) edge preparation abrasive waterjet cutting, top-to-bottom, $P_L$ 15 kW, $v_w$ 2.75 m/min, fpp -7 mm.

4.2.2 Laser cutting

With laser oxygen cutting (LOC) it is possible to prepare straighter cutting kerf than with abrasive waterjet cutting. See comparison between AWJC, part A, and LOC, part D, in next Fig. 5. In this Fig. 5 all the workpieces are in top-to-top set up. With LOC, the cutting kerf is not opening from the top side of the workpiece making the bevel. When comparing the top side and root side of the workpiece the air gap is similar and the groove is straight. This is beneficial for keeping the melt pool in the groove. Top and root side of the weld is of rather good quality, unlike in the joint prepared with AWJC edges (part B), in which the top and root side of the weld are underfilled. In LOC groove has channel-like gap in the middle of the thickness. This perhaps has beneficial influence on the movement of the melt during welding process and is decreasing the pressure in the melt.

Fig. 5. Abrasive waterjet cutting compared to laser oxygen cutting. Setup 6. A) joint groove used in B, abrasive waterjet cut, top-to-top. B) $P_L$ 15 kW, $v_w$ 2.5 m/min, fpp -7 mm. C) edge preparation, top-to-top, $P_L$ 15 kW, $v_w$ 2.5 m/min, fpp -4 mm. D) joint groove used in C, laser oxygen cut, top-to-top.
4.2.3 Machining and grid blasting

In next Fig. 6 the cross sections of welds made with machined groove edges are compared to cross sections of welds made with grid blasted machined groove edges. Grid blasting increases the efficiency of laser welding in case of machined groove edges. With grid blasting, the penetration depth of 15.3 mm, obtained with machined groove edges and 16 kW laser power and 1.2 m/min welding speed, (part A of the Fig. 6), can be increased to 21.4 millimeters (part B of the Fig. 6), a very significant increase is 39.9 %. As also partial penetration (70 %, 13.7 mm) of weld with 1.5 m/min welding speed and 16 kW laser power (part C of the Fig. 6) can be brought to full penetration (19.7 mm) with grid blasting (part D of the Fig. 6).

**Fig 6.** Edge preparation with machining and machining with grid blasting. Setup 6. A) edge preparation milling, $P_L$ 16 kW, $v_w$ 1.2 m/min, fpp -7.5 mm. B) edge preparation milling and grid blasting, $P_L$ 16 kW, $v_w$ 1.2 m/min, fpp -7.5 mm. C) edge preparation milling, $P_L$ 16 kW, $v_w$ 1.5 m/min, fpp -7.5 mm. D) edge preparation milling and grid blasting, $P_L$ 16 kW, $v_w$ 1.5 m/min, fpp -7.5 mm.

In next Fig. 7 the penetration depth of partial penetration welds made with machined groove edges and machined edges with grid blasting are presented. All the welds shown in Fig. 7 are made with same transfer fiber to have constant focal point diameter. The difference in penetration depth is decreasing with increase in the welding speed. These differences of penetration depth can be up over 30 %.

**Fig. 7.** Effect of grid blasting of the joint edge on penetration depth. Setup 6. Machined grid blasted (mgb), machined without grid blasting (m). Fpp -6 mm and -7.5 mm, laser powers 12 kW and 16 kW.

As overall conclusion of this test serie it can be said that every groove edge preparation method has its own characteristics and parameter maps for welding process, which differ from method to method. Most effective influence can be made with grid blasting to the machined
joint edge. With this method the penetration depth of the weld can be increased significantly. Wide weld with low penetration can be modified towards narrow weld with deep penetration. Still good quality welds can be produced with different kinds of edge surfaces.

4.3 Effects of joint geometry to bead reinforcement and keyhole geometry

The comparison of weld bead size reinforcement was studied by varying welding speed and focal point position. Welding speeds from 2 up to 5 m/min and four different fpp’s from 0 to -6 mm were tested. The laser used in these experiments was a 5 kW disc laser. An x-ray videography system was built into the workstation allowing real time x-ray videography during laser welding process. Fig. 9 shows reinforcement with bead on plate and bevel filling capability with butt joint. Due to the bevel of the edges on the butt joint a new term was taken into comparison, bevel filling capability, which means that the area of the bevel is measured with the real reinforcement and then combined to make the value.

Fig. 9. Left: reinforcement area with bead on plate [mm²], setup 4, and right: filling capability with butt joint [mm²], setup 5.

As an example, calculation at 2 m/min with the fpp -2 mm resulted in 0.28 mm² reinforcement completely above the surface. Then the bevel area of 0.29 mm², this was measured from several samples and an average bevel area was calculated, is added to this value and result bevel filling capability is 0.57 mm². This is the joint cross section area possible to fill using these welding parameters when the joint edge is a laser cut edge. Laser cut I-butt joint is common in industry due to large volume of laser cutting machines and services. Similar joint type was formed in orbital laser cutting experiments performed and published by two of the authors, Vänskä and Salminen [9]. This also works for bead on plate but with the exception that the bevel area is 0 mm². This can almost be compared to a machined I-butt joint which is suggested by Sokolov et al. [10]. The welding speed of 2 m/min and fpp -2 mm with bead on plate configuration resulted in 0.89 mm² reinforcement. The difference in the bevel area affects the filling capability and the values are not directly comparable, but the effect of parameters is very similar especially at welding speed of 2 m/min. This results that highest filling capability is with slow welding speeds and focal point position also affects the filling and bead reinforcement formation. Fig. 10 shows welding speed 2 m/min welds with fpp -2 mm.
The 2 m/min welding speed fills the groove relatively well, resulting in class B weld, even if there is, in this case, a large bevel. This of course depends what is the comparison; for example conventional welding methods commonly uses V- or Y- joint. In those cases the filling capability depends on the angle of the bevel in addition to the welding speed. The weld penetration difference is very small, 6.1 mm on bead on plate and 6.5 mm on butt joint resulting in 6.4 % difference in the penetration. The reinforcement area difference was 36 % which is a significant increase. The weld shape is also very similar with both types; the solidification happened in two phases, root solidification is the first phase and top part solidifies on the second phase. It is visible as “two weld” effect on the top half of the weld. The weld form itself is same in both cases, but this also shows that material has very strong flow from the lower part to the surface. Also the material bends on the top side and creates larger weld bead. When the welding process is observed with the x-ray videography system, the keyhole geometry is visible with both joint types and also the filling of the bevel is visible in Fig. 11.

As shown, both of the keyholes are similar shape but the butt joint keyhole is slightly deeper, 5.26 mm, compared to that of bead on plate keyhole’s depth of 4.81 mm. The difference is approximately 9 % which is slightly more than difference in penetration of the welds. This is based on only one test set and spiking can occur, affecting both depths separately. The keyhole is an average from almost the whole weld length and only very short time spiking is removed due to the averaging. The weld depth is measured from a random
location in which the welding process has been stable. From the x-ray videos it is possible to
determine keyhole oscillations and spiking. In the case of 2 m/min weld the oscillations of the
keyhole was 4 % of the keyhole depth. Another difference is melt flow to the top part of the
sample, which is visible in the x-ray images as a dark area near the top surface, which is the
bevel filling area. The unwelded part of the sample is on the right side of the keyhole. The
reason that the left side, the melt pool and the weld, is lighter, is the reduced thickness, i.e.
width of the sample that the x-ray penetrates through. The material flows from the bottom to
the top side and creates bead reinforcement. This is also visible in the sample width when the
total width is measured before and after welding. The differences are relatively small but do
have an effect. Fig. 12 shows sample width before and after welding and relative difference.
The keyhole geometry also changes when there is a butt joint. The largest vertical cross-
section area is lowered in the butt joint, which increased butt joint weld width at the root side.
The root width increased 18 %, 0.65 mm on butt joint compared to 0.55 mm on bead on plate.
The width was measured at 10 % of the weld depth from the bottom to remove “rounding”
effect.

![Sample width before and after the welding.](image)

The width reduction affects mainly on the first quarter of the depth and it is relatively
small. The maximum width reduction is on the surface, which causes increased tension due to
uneven width reduction through the whole thickness. The thickness was measured from
unwelded section but it had pressure so the air gap was near 0 mm in the middle where there
was no bevel. Although the width reduces, it does not fully explain the reinforcement area.
Bubble formation and especially pore formation does increase the reinforcement and bubbles
inside the melt pool probably increases melt flow and brings more melt to the surface, this
probable effect will be studied further.

### 4.4 Fillet welds

One side single pass fillet welds were made in 10 mm thick low alloy steel Ruukki Laser
S355MC (SFS-EN 10025-2) using setup 8. The plates were joined in T-joint configuration in
PA (1F) welding position. The thicknesses and material of the web and the flange were the
same. Plates were laser cut to pieces of 350 x 100 mm in size and grid blasted. Prior to
welding the steel plates were tack welded together in a form of inverted T. The length of the weld was 165 mm. Aim of this test series was to obtain full penetration with acceptable weld quality on both sides of the joint.

First factors affecting the weld formation studied were the position of the focal point. \( \text{Fpp} - 2 \text{ mm} \) was found to be more efficient than \( \text{Fpp} + 2 \text{ mm or zero} \). As shown previously in this paper, the air gap in the joint has influence on penetration depth in butt joint. Results of current test series show that good quality joints with deep penetration are possible also when plates are directly pressed against each other; in fact, the penetration is deeper with autogenous laser welding than in case of hybrid weld produced with same laser parameters. Fig. 13 shows welds made in 10 mm thick S355 with beam angle 10° to surface of the flange, weld A (hybrid weld) has penetration in joint groove 6.5 mm, and B (autogeneous laser weld) 8.2 mm.

**Fig. 13.** Fillet welds made in S355, thickness 10 mm, \( P_L 8 \text{ kW}, \nu_w 0.75 \text{ m/min, fpp} -2 \text{ mm. A) laser–MAG hybrid weld (} P_{Arc} 2.4 \text{ kW, filler wire feeding speed } 7.2 \text{ m/min), beam angle 10°; B) laser weld, beam angle 10°; C) laser weld, beam angle 6°. Setup 7.**

Comparison of Fig. 13 A and B shows that penetration is deeper in autogeneous laser weld, as all of the laser energy is absorbed in base material to form the joint, while in hybrid process a fraction of laser energy is distracted by filler wire and arc, thus less is available for creating penetration the other explanation is that extra material of the arc welding is bringing too much material to the joint preventing the keyhole formation as efficiently. Of course, building actual fillet with certain a-dimension is not possible using autogeneous laser welding, nonetheless, when taking EN-ISO 5817-2006 standard of quality of the arc processes as reference, the length of effective throat of laser welded T-joints produced in this study exceed the demands set for leg size of arc welds in same thickness and also fulfill the criteria of quality class B for non-complete penetrating welds. One way to increase penetration in case of T-joint is to lower the angle between flange and the laser beam, but decrease as small as 4° results in undercut and blowout, as shown in Fig. 13 C, and the shift of the whole parameter window. The low angles are very seldom suitable for welding of real applications.

For a constant laser power (9.5 kW), the welding speed was varied at 0.25 m/min steps from 0.5 m/min to 2.5 m/min, resulting in welds displayed on Fig. 14. Welding speed lower than 1.0 m/min produces full penetration, but the keyhole is not stable and as Fig. 14 A and B show, blow-outs and undercuts are formed. The melt is pushed through the joint to root side of the weld and top side has lack of penetration. Welds C - I shown on Fig. 14 all have acceptable visual quality; depth of penetration gradually decreases from 7.4 mm at \( \nu_w 1 \text{ m/min to } 6.1 \text{ mm at } \nu_w 2.5 \text{ m/min or } 17 \% \) during 6 steps of 0.25 m/min speed increase in each step. It might not seem as much, but changes of width of the weld and fusion zone are significantly greater. Weld C has fusion zone of 17.9 mm² and weld I 10.7 mm², which is a 40 % decrease, thus mechanical properties, performance under stress and load carrying ability of the joints can be assumed to differ vastly as well.
From Fig. 13 and Fig. 14 can be seen that considering fillet welds, the process parameters such as welding speed and laser angle have much effect on the penetration. Any case the joint forms a light trap naturally and utilizes the laser power efficiently. The beam penetrates the flange and if beam-metal interaction time is too long, the melt is unable to solidify and escapes from back of the groove, and as time decreases, so do the width and depth of the penetration. Overall, groove edge preparation with laser oxygen cutting is suitable for autogeneous laser fillet welding and tightly fitted parts, however joint preparation has less significance in welding of T-joint than in case of butt joint.

5 Conclusions

In this paper the features and applicability of different edge preparation methods were discussed and comparison of their efficiency was made. It can be concluded, that the edge preparation influences mainly to the size of the air gap in the groove. The bead reinforcement depends on the geometrical features of the joint fit-up, welding speed and focal point position. When comparing the bead on plate testing to the experiments with butt joint configuration, differences in keyhole behavior and penetration depth are noticed. With bead on plate test configuration, the change is almost linear, but with butt joint the change is steady but not linear. In butt joint configuration the edge preparation method is an important factor for total efficiency of the welding process. In fillet joint configuration, the edge preparation is not as important as in butt joint, as the basic welding parameters affect the final result more than the properties of the groove edges.

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References


ADVANCES IN GASES AND GAS MIXTURES FOR LASER CUTTING AND WELDING

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Abstract

The development of new industrial laser sources have caused a renewed interest in research regarding optimisation of laser cutting and welding processes. Earlier, CO₂ lasers were almost exclusively used for industrial cutting and welding. Laser source development has created a need for high purity laser resonator gas mixtures, beam path purging gases have also been introduced. The new industrial fibre and disc lasers are causing a shift in technology use in manufacturing industry, as these lasers seem likely to replace CO₂ lasers for thin steel sheet cutting, and have already done so for many laser welding applications. The difference in wavelength between CO₂ lasers and fibre and disc lasers has substantial influence on the welding and cutting processes. Fibre and disc lasers are more efficient at cutting thin steel sheet than CO₂ lasers. Different factors are determining the maximum cutting speed, depending on the type of laser source used. In laser welding, plasma effects become less relevant than with CO₂ lasers and consequently the choice of gas needs to be made taking different effects into account, leading to gas mixtures optimised for fibre and disc laser welding.

Key words: laser, lasers, gas, gases, process, fibre, disc, CO₂, welding, cutting, processing, helium, argon, oxygen, purity, flow, development, oxidation, Linde, AGA, BOC

1. Introduction

Beam quality and electrical efficiency of fibre lasers* have been improved drastically during recent years. Fibre lasers now reach a brightness high enough for cutting and welding, at powers of 10 kW and higher. This has led to a breakthrough for fibre lasers involved in cutting and welding applications. Improvements in beam quality, lifetime as well as reduction in cost has lead to an increased interest for diode lasers. Brightness of diode lasers is lower than that of fibre and CO₂ lasers though, and the use has so far been restricted to conduction welding, cladding and surface treatment processes that do not require a small laser spot size and high brightness.

*in the following text fibre and disc lasers will be referred to as fibre lasers only
2. Future development

The wavelength of the fibre laser beam is app. 1.05 -1.07 µm at high beam quality. The shorter wavelength makes it possible to transfer the beam via a fibre to the cutting or welding head. It is also possible, in addition, to switch the laser beam between two or more work stations in a time sharing mode. The electric efficiency of the fibre laser is more than 8 to 10 times higher than that of a conventional used Nd:YAG laser. Even compared with a CO₂ laser the electric efficiency is at least double and often even better. Fibre lasers for material processing are today available with laser power above 10 kW.

The wavelength of CO₂ lasers at 10.6 µm which means that the beam cannot be transmitted through a fibre, instead mirrors must be used. From this point of view it could easily be concluded that the days of this laser type are over. But in some important applications, like thick sheet cutting and some areas in welding, it still offers substantial advantages, and will very likely stay in the market for some time to come. CO₂ lasers are available for cutting and welding with a power up to 12 kW or more.

The wavelength of diode lasers is typically 0.9 – 1.07 µm with a low beam quality compared to fibre and CO₂ lasers. CO₂ and fibre lasers normally have a round shaped beam while the diode laser beam is rectangular. The divergence angles also differ in the horizontal and vertical direction. This means that while it is not possible to focus the beam to a sufficiently small spot size for cutting or welding, the diode laser is more suitable for heat conductive welding and surface treatment processes. For such applications, the diode laser is the preferred laser source, since this laser type has the highest electric efficiency in combination with the lowest cost. Maximum power of diode lasers for industrial use today is typically 4 – 6 kW.

The question which laser will be used in the future depends on the application. Most probably there is no single best solution for all applications. Fibre lasers most probably will dominate the thin sheet cutting, and the major part of the welding applications. CO₂ lasers, being the most flexible tool for cutting and welding will continue to play a significant role in metal fabrication. The future is difficult to predict, and opinions are divided about which laser type will dominate in the future. Taken in to account, the growth of laser applications, it is likely to assume that the market for all three laser types will continue to grow. The use of CO₂ lasers will grow from a big base with lower growth rates, and fibre lasers will grow aggressively from a smaller base.

The expected change of laser sources – from CO₂ lasers to fibre diode lasers for cutting and welding will result in changed requirements for laser cutting and welding process gases, a change that is driven by the different physical processes that take place at the different wavelengths.

3. Interactions between the laser and the material for different wavelengths

The absorption of the laser beam by a metal depends on the radiation wavelength and polarisation, and also on the angle of incidence of the beam on the metal surface.
For the wavelengths of fibre and diode lasers the absorption at room temperature and at vertical angle of incidence onto the metal (i.e. 0°) is for the most metals higher than for the CO₂ laser wavelength (Figure 1).

**Fig. 1. Absorption as a function of wavelengths for various metals at room temperature**

The absorption rate changes with increasing material temperature, and when the beam no longer hits the surface vertically. While absorption usually increases with temperature, the effect from varying angle of incidence is more complex. In the laser cut front, or in the laser welding keyhole, the angle of incidence can come close to 90° (i.e. at glancing angle, laser beam almost parallel to the metal surface). In figure 2, the relation between angle of incidence and absorption is shown at a temperature which is typical for laser cutting. The angle at which optimum absorption is reached is called the Brewster angle.
4. Laser cutting processes

Laser radiation at 10.6 μm and in the spectral area around 1 μm (fibre and diode laser) show different process behaviour in laser cutting.

In inert gas cutting of mild steel and stainless steel the angle of the cut front becomes less steep as the cutting speed is increased, see figure 3. For a CO₂ laser, the optimum cut angle for maximum absorption is reached at relatively low speed. Even though the speed could be increased by increasing the laser power, the cut front angle goes away from the Brewster angel, and much of the radiation is reflected.

The Brewster angle for the radiation from a fibre laser is much smaller than from the CO₂ laser. This means that maximum absorption is reached at a less steep cut front, and therefore at a higher speed. The differences in Brewster angle between the two lasers means that for
thin sheet a fibre laser can cut up to 3 times faster than a CO₂ laser, because it is a much more efficient process thanks to the smaller Brewster angle.

For thick metal, the speed must be reduced and the cut front angle becomes quite steep again, meaning that CO₂ lasers produce good, efficient cuts in thick sheet. Fibre lasers on the other hand operate away from the Brewster angle in thick sheet, and the cutting process relies on multiple reflections inside the cut. While the process is still efficient, the cut quality is more or less destroyed in thicker material.

Laser cutting of mild steel with oxygen does not depend that much on the wavelength. This is explained by the fact that, as the material thickness increases, more and more of the energy for the cutting process is provided by the combustion of the iron. This is why cutting speed and quality when using oxygen as cutting gas is not depending on the choice of laser source.

To conclude, fibre lasers are the best choice when cutting thin sheet (up to around 4-6 mm) with nitrogen, while CO₂ lasers give certain advantages when cutting thick mild steel. One important issue is the purity requirements of the oxygen, as will be shown below.

Figure 3 shows differences in cutting speed for cutting thin mild or stainless steel sheet.

![Figure 3](image_url)

**Fig. 3.** Cutting speed - mild steel with nitrogen, clearly showing the higher cuttings achieved with fibre lasers on thin sheet.

A comparison between 3 kW and 6 kW fibre and CO₂ lasers seem to be a logical choice since a 3 kW fibre laser and a 6 kW CO₂ laser area present comparable laser types for high-end laser cutting machines at similar cost equipped. Even if the laser power of the CO₂ laser would be doubled, the cutting speed of the fibre laser can not be reached below 3 mm. Above
3 – 4 mm the speed advantage of the fibre laser is getting smaller, also the cut quality is reduced.

5. Laser welding processes

The main difference for laser welding between the two laser types is that 1 μm radiation cannot sustain a plasma (because the plasma is transparent to this wavelength), while 10.6 μm is strongly absorbed by a plasma, and sustains it. Thus, a CO₂ laser produces a plasma-filled keyhole, and a fibre laser does not. The consequence from this is that a fibre laser produces a narrower weld, with a smaller heat affected zone and a higher speed. One disadvantage is that the fibre laser tends to produce more spatter in some metals. The reason for this is that the laser radiation hits the keyhole wall directly, not indirectly through the heat transfer from a plasma as in the case of welding with a CO₂ laser.

The fact that a fibre laser does not produce a plasma like the CO₂ laser does, affects the choice of process gas, as will be shown later.

6. Process gases for laser cutting

6.1 Nitrogen cutting (mild steel, stainless steel)

The nitrogen cutting process relies on mechanical means to remove the molten metal. In order to obtain sufficient energy a high pressure is required. There are also demands on the purity of the gas since oxygen contamination of the nitrogen gas will produce oxidised cut edges in stainless steel materials. Figures 4a and 4b show typical cutting results with various purity levels of nitrogen. Generally, in order to get good clean cuts, a purity of at least 99.995% is required although this depends on the sheet thickness. The thinner material, the slower process which increases the sensitivity for oxidation.

![Image of cutting results with nitrogen purity levels](image_url)

Fig. 4a. Laser cutting of stainless steel with different nitrogen purity for 3 mm stainless steel.
6.2 Oxygen cutting of mild steel

One important part of this process is the exothermal reaction between iron and oxygen. Laser cutting with oxygen mostly used for cutting of mild steel, the use of oxygen for cutting of stainless steel is from obvious reasons not so common.

Fibre and CO₂ lasers show no major difference in terms of cutting speed when using oxygen as process gas. The kerf width achieved with fibre lasers is usually a little bit more narrow than with CO₂ lasers. Figure 5 shows that there is only a small difference in cutting speed for both laser types with oxygen. An increase in laser power will not have any positive effect on oxygen-assisted laser cutting since the cutting speed is limited by the reaction time of iron and oxygen. This reaction speed can not be improved by using more laser power. Moreover, a too high laser power results in inferior cut quality due to the excessive power.
Fig. 5. Cutting speed with oxygen-assisted laser cutting

Of important factor during laser cutting with oxygen is the purity of the oxygen. The main impurity in oxygen is often argon (and some nitrogen). When cutting thick mild steel sheet, a substantial proportion of the oxygen in the process gas is used during the combustion process. This means that deeper into the cut, the argon concentration increases to such an extend that it is beginning to slow down the cutting process and thereby becomes the limiting factor of the process. This effect is so strong, that it can mean a difference of up to 30% in cutting speed between 99.5% purity oxygen, and 99.95% - see Figure 6.

Using high purity oxygen as process gas does not only increase the cutting speed, it also improves the cutting process when cutting mild steel sheet thicknesses at the maximum capability of the laser cutting machine. This means reduced scrap volumes, higher up-time and easier switch between steel sheets produced by different manufacturers.

With the introduction of fibre lasers, the use of oxygen for laser cutting has become more and more restricted to thick mild steel cutting. This is exactly where the purity of the oxygen is most important.
7. Process gases for laser welding

The process gas in laser welding has several tasks. One of the main tasks is to optimise the weld quality and weld performance. Prevention of oxidation of the molten material, and also prevention of spatter during the welding process are additional tasks. Also, during CO₂ laser welding the process gas must suppress plasma formation above the keyhole. This is not a problem with fibre laser welding, as fibre laser radiation cannot sustain a plasma in any gas. Hence, the process gas is determined by the laser wavelength and the material specifications. In some cases addition of active components to the process gas may change the weld pool behaviour as well as the metallurgical appearance, for example by changing the surface tension of the molten material, or to introduce carbon or oxygen into the weld.

CO₂ laser radiation can, as mentioned before, sustain a plasma in the process gas, something which must be avoided. Therefore the most common process gases for high power CO₂ laser welding are helium or helium based mixtures. Helium atoms are much more difficult to ionise and can not form a plasma as easily as argon. Helium also offers advantages due to its high thermal conductivity which increases the cooling rate of the weld. Pure helium works in most cases in terms of creating a stable welding process. If the laser power is not too high (below approx 6-8 kW) helium can be mixed with other gases in order to reduce helium consumption and sometimes also to improve weld performance. But, for the highest power CO₂ laser (12 kW and more) pure helium is usually essential in order to avoid excessive plasma formation.

Figure 7 demonstrates the effect on gas choice. A 6 kW CO₂ laser with pure argon as process gas results in an excessive plasma formation above the surface. Almost no laser energy reaches the material due to absorption in the plasma cloud.

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<th>Laser Power 2 kW</th>
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<td>O₂ Purity 99.5%</td>
<td>O₂ Purity 99.5%</td>
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<td>Cutting speed 1.10 m/min</td>
<td>Cutting speed 1.10 m/min</td>
<td>Cutting speed 0.85 m/min</td>
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Fig. 6. Cut edge quality with 99.5% oxygen and 99.95% oxygen in 12 mm mild steel. In order to get good cuts, the speed must be reduced from 1.1 m/min to 0.85 m/min with a 2 kW laser source.
The use of pure helium results in a good weld quality, a large process window for welding speed and laser power. As seen above, the process gas could be further optimised by replacing some of the helium by argon. This will improve the gas flow towards the weld. Also, a small addition of carbon dioxide will improve the viscosity of the melt and add energy to the process due to the release of oxygen. The example shows a 30% welding speed increase by replacing a major part of the helium with argon and CO₂.

Since the fibre laser cannot sustain a plasma, the choice of process gas will be defined by the process properties and the interaction with the material. The use of helium is not essential in this case. In normal cases argon, or argon based process gases are used.

The absence of plasma formation during fibre laser welding makes the process, at least when discussing the chemical behaviour of the process gas, more similar to the TIG welding process. Helium additions may be used due to the benefits of the high heat conductivity. Also the spatter formation may be reduced by adding helium to the gas.

8. Conclusions

A technology change is taking place in terms of laser processing equipment. For thin sheet cutting and welding applications, fibre lasers at a wavelength of around 1 μm are replacing CO₂ lasers which operate at 10.6 μm. In laser cutting, this change is driven by higher cutting efficiency, in laser welding fibre delivery makes 3-D welding applications very easy to implement is an important factor. One advantage with fibre lasers is also that argon-based process gases can be used. This is possible since there is no plasma formation to suppress as in the case of welding with CO₂ lasers.

In cutting, CO₂ lasers will still be mostly used for thick mild steel oxygen cutting, an area where oxygen purity becomes more important. CO₂ lasers will also remain to be used for cutting thick stainless where the purity of the nitrogen is critical.
THE INFLUENCE OF SHIELDING GAS ON THE PROPERTIES OF LASER WELDED STAINLESS STEEL

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²Luleå University of Technology, Department of Engineering Sciences and Mathematics, SE-97 451 Luleå, Sweden

Abstract

Argon is generally used as shielding gas for laser welding. As Argon is an inert gas it does not have influence on the microstructure of the weld material or on the heat input of the weld, whereas nitrogen has solubility to austenite. Therefore nitrogen as an interstitial atom increases the hardness of the weld. This has been detected when comparing the hardness profiles of nitrogen shielded welds of austenitic stainless steel with Argon shielded welds. Nitrogen as the shielding gas can compensate the softer structure of the weld in work-hardened and nitrogen-alloyed steels.

For the duplex stainless steel grades the nitrogen additions promote formation of austenite in the weld, which decreases the risk of lowered toughness. In this study the influence of nitrogen shielding gas on the strength of the laser weld has been examined. The strength of laser welds of nitrogen alloyed work-hardened stainless steel seems to be slightly better when using nitrogen shielding gas compared to welds for argon shielding gas. It has also been verified that nitrogen as shielding gas decreases the risk of weak toughness of laser welded duplex stainless steel.

Keywords: laser welding, stainless steel, shielding gas

1 Introduction

Nitrogen alloying is used in order to increase the strength of Austenitic stainless steel. The heat influence of welding can cause a nitrogen loss of the weld. The study will determine the influences of Nitrogen shielding gas on the hardness profiles of the laser weld and also on the strength properties of Nitrogen alloyed austenitic stainless steels.

In addition the study will compare the formability of Lean duplex stainless steel laser welds which are welded with Argon and Nitrogen stainless steel. Low austenite content of a weld can cause a nitride precipitation which has a deleterious defect to the corrosion properties and toughness [1]. The target of the study is to increase the austenite content of the
weld by using Nitrogen as shielding gas and backing gas [2]. The nitrogen as alloying element favours austenite according to WRC-92 equations (1) and (2) [3].

\[
\text{Ni-ekv} = %\text{Ni} + 35*%\text{C} + 20*%\text{N} + 0,25*%\text{Cu} \\
\text{Cr-ekv} = %\text{Cr} + %\text{Mo} + 0,7*%\text{Nb}
\]

When the austenite content increases also the Ni-ekv/Cr-ekv ratio increases. The heat influence of welding may cause a nitrogen loss of nitrogen alloyed steels. On the other hand the high solidification rate increases the ferrite content of weld, because the austenite becomes solid in the weld by diffusion. Therefore the LDX 2101® welds were welded by using parameters with different heat input.

## 2 Test welds and materials

The test welds were made by using a 4 kW disc laser as power source. A 300 mm optics was used for all welds. The shielding gas was blown over the weld by using pipe before the keyhole. In addition a 60 mm shielding gas nozzle was employed behind the keyhole. The root side was also shielded with the same shielding gas, see Fig. 1.

*Fig. 1* The shielding gas arrangements

The welded materials were work-hardened ¼ hard EN 1.4372 type austenitic stainless steel and LDX 2101® type duplex stainless steel.

The laser weld parameters of EN 1.4372 material have been chosen so that the heat input is as low as possible in order to get the best strength of the weld. The shielding gas was varied in order to show the difference of shielding gas influence on the weld. The laser weld parameters of LDX 2101® are chosen in order to show the influence of heat input and nitrogen shielding gas on the austenite content of the weld. The weld parameters are shown in Table 1.
Table 1. The weld parameters used for the materials EN 1.4372 and LDX 2101®

<table>
<thead>
<tr>
<th>Material</th>
<th>Laser power kW</th>
<th>Speed, m/min</th>
<th>Focal point level, mm</th>
<th>Shielding gas and flow rate</th>
<th>Energy input, J/mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>EN 1.4372 2,3 mm</td>
<td>4</td>
<td>7</td>
<td>-1,0</td>
<td>Argon 30 l/min</td>
<td>34,3</td>
</tr>
<tr>
<td></td>
<td>4</td>
<td>7</td>
<td>-1,0</td>
<td>Nitrogen 30 l/min</td>
<td>34,3</td>
</tr>
<tr>
<td>LDX 2101® 1,5 mm</td>
<td>3</td>
<td>9</td>
<td>-1,0</td>
<td>Argon 30 l/min</td>
<td>20,0</td>
</tr>
<tr>
<td></td>
<td>3</td>
<td>4,5</td>
<td>-1,0</td>
<td>Argon 30 l/min</td>
<td>40,0</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>1,5</td>
<td>-6,0</td>
<td>Argon 30 l/min</td>
<td>80,0</td>
</tr>
<tr>
<td></td>
<td>3</td>
<td>1,5</td>
<td>-9,0</td>
<td>Argon 30 l/min</td>
<td>120,0</td>
</tr>
<tr>
<td></td>
<td>3</td>
<td>9</td>
<td>-1,0</td>
<td>Nitrogen 30 l/min</td>
<td>20,0</td>
</tr>
<tr>
<td></td>
<td>3</td>
<td>4,5</td>
<td>-1,0</td>
<td>Nitrogen 30 l/min</td>
<td>40,0</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>1,5</td>
<td>-6,0</td>
<td>Nitrogen 30 l/min</td>
<td>80,0</td>
</tr>
<tr>
<td></td>
<td>3</td>
<td>1,5</td>
<td>-9,0</td>
<td>Nitrogen 30 l/min</td>
<td>120,0</td>
</tr>
</tbody>
</table>

3 Results

3.1 Hardness profiles

The hardness profiles of EN 1.4372 laser welds with different heat input and shielding gases are shown in Fig 2.

![Hardness profiles of 2,27 mm EN 1.4372 TR laser welds with different shielding gases and energy input](image)

Fig. 2 The hardness profiles of work-hardened EN 1.4372 steel laser welds with different heat input and shielding gases [4].
3.2 Tension tests

Tension tests are made for the EN 1.4372 Steel. The results are an average of four tension tests. The tension test results of EN 1.4372 steel laser welds are shown in Table 2.

Table 2. Tension test results of EN 1.4372 TR Laser weld compared with base materials

<table>
<thead>
<tr>
<th>Shielding gas</th>
<th>Rp0,2% [MPa]</th>
<th>Rult [MPa]</th>
<th>A 50 [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Nitrogen</td>
<td>682,25</td>
<td>821,25</td>
<td>25,90</td>
</tr>
<tr>
<td>Argon</td>
<td>677,50</td>
<td>821,25</td>
<td>27,65</td>
</tr>
<tr>
<td>Base material</td>
<td>693,50</td>
<td>870,50</td>
<td>32,00</td>
</tr>
</tbody>
</table>

The tension bars of LDX 2101® all broke in the base material, so the tension test result of laser welds were the same as for the base material.

3.3 Fatigue tests

Fatigue tests were made for laser welds of 1.5 mm thick EN 1.4372 TR steel by using an Instron 8802 tension compression test machine. A buckling support was used in order to prevent the buckling in compression. The fatigue bars were laser-cut. Then the necking parts of the bars were milled. The fatigue strength of laser welds of 1.4372 steel is shown in Fig. 3.

Fig. 3 S/N-curve on fatigue test results of EN 1.4372 TR steel laser welds, welded by using different shielding gases compared with the base material [4].
3.4 Metallography

The cross sections in Figs 4.a),b) have been etched in NaOH liquid by using 2,5 V voltage for 10 seconds. Then the ferrite areas corrode more and become darker. Phase contents of LDX 2101® have been measured from the cross sections by using an area measurement system of the Keyence VHX 2000 microscope. The austenite contents of the welds are shown in Table 3.

**Table 3. The austenite content of LDX 2101® Laser welds**

<table>
<thead>
<tr>
<th>Shielding gas</th>
<th>20 J/mm</th>
<th>40 J/mm</th>
<th>80 J/mm</th>
<th>120 J/mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>N₂, % austenite</td>
<td>8,10</td>
<td>11.82</td>
<td>28,78</td>
<td>17.28</td>
</tr>
<tr>
<td>Ar, % austenite</td>
<td>1,33</td>
<td>11,40</td>
<td>16,84</td>
<td>15,56</td>
</tr>
</tbody>
</table>

**Fig 4 a) LDX 2101® laser weld 20 J/mm, Argon as shielding gas**

**Fig 5 a) LDX 2101® laser weld 20 J/mm, Argon as shielding gas. Brittle fracture at 30° bending angle**

**b) LDX 2101® laser weld 20 J/mm, Nitrogen as shielding gas. 180° bending**
3.5 Bending tests

The bending tests were carried out by using manually operated simple bending machine. The bending test results are shown in Table 4.

Table 4. The maximum bending angles of LDX 2101® laser welds for different parameters

<table>
<thead>
<tr>
<th>Shielding gas</th>
<th>20 J/mm</th>
<th>40 J/mm</th>
<th>80 J/mm</th>
<th>120 J/mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>N₂, max bending angle,°</td>
<td>180</td>
<td>180</td>
<td>180</td>
<td>180</td>
</tr>
<tr>
<td>Ar, max bending angle,°</td>
<td>30-40</td>
<td>180</td>
<td>180</td>
<td>180</td>
</tr>
</tbody>
</table>

4 Discussion

The use of Nitrogen shielding gas has advantageous impact on the strength of some stainless steels. According to Fig. 1 the hardness of the welded area of the work hardened stainless steel becomes higher when welding with Nitrogen shielding gas. The yield strength seems to be also a bit higher in Nitrogen shielded welds, Table 2. The use of nitrogen as shielding gas can compensate the nitrogen loss of nitrogen-alloyed steel [1]. The nitrogen as interstitial atom has a big effect to the strength of austenitic steel. Therefore slight changes of nitrogen content can have a notable effect to the strength of the weld. In this study the nitrogen content of the weld has not been measured, but it should be interesting to measure the nitrogen content through the weld and base material. The fatigue strength of Nitrogen-shielded welds seems to be better. Certainly we must take into account that the geometry of the weld has a main influence on the fatigue strength.

Welding of 1,5 mm LDX 2101® steel by using 20 J/mm energy input causes 98,67 % ferritic microstructure of the weld due to short diffusion time in solid state, see Table 3. This causes deleterious defects to the toughness and also corrosion properties of the weld. According to bending test result the welds were very brittle in case of welds of lowest heat input with argon shielding gas, see Table 4. The reason for this is the almost pure ferritic microstructure of the weld area, which has a tendency to nitride precipitation causing toughness problems [3],[6]. Higher heat input increases the austenite content of the weld and at the same time increases the toughness of the weld, too.

According to Aida [5] the impact toughness decreases when the ferrite content increases over 60 %. The impact toughness at –46 °C for the 68,62 % ferrite weld is about 33 J whereas for 98,67 % ferrite content it is 0 J.

According to Ogawa et al. [6] the pitting corrosion rate is lowest at 50 % ferrite (2 g/m² h). In case of laser welds with heat input of 80 J/mm using nitrogen shielding gas the ferrite content is 68,62 % so that the pitting corrosion rate is about 4 g/m² h. Then, if the ferrite content increases, the pitting corrosion rate increases rapidly.

For the future it is useful to compare the fatigue strength of EN 1.4372 steel by using polished fatigue bars so that the geometry effect of the weld is eliminated. Post heat treatment of LDX 2101® laser welds should be made in order to increase the austenite content for better pitting corrosion and toughness properties.
5 Acknowledgements

The research was done during the programme Interreg IV A Nord, project PROLAS, no. 304-58-11. The authors would like to acknowledge financial support from the Interreg IV A Nord program, Lapin Liitto and Länsstyrelsen Norrbotten as the national funding bodies. We would also like to thank the private financiers of the PROLAS-project. The authors would especially like to express their gratitude to the company Outokumpu Oyj for providing the test materials in addition to their valuable support during the research.

6 References


Laser hybrid welding in heavy section manufacturing – perspectives, limitations and challenges

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Abstract:
Laser-arc hybrid welding is today a well-established process in the welding of structural steels. In the case of CO2 laser hybrid welding it has for some time been realized that because of the beam-plasma interaction the practical achievable weld thickness is limited to below approximately 15 mm. Due to their shorter emitted wavelength the fiber and disc lasers do not show the same intense beam-plasma interaction, which has given rise to the hope, that much larger penetrations may be achievable here. Results seem however to show that also these new laser source types under realistic heavy section manufacturing situations have a similar quite limited ability to reach penetration depth in steels and stainless steels in excess of approximately 25 mm. The reasons for this are discussed.

For many heavy section welding applications as e.g. in wind turbine tower manufacturing the submerged arc welding process (SAW) is used extensively and this often causes severe problems with mechanical and metallurgical properties as well as with distortion. For structures exceeding 25mm in thickness the substitution of the submerged arc process with a root pass welded by laser-arc hybrid welding followed by filler passes using either the same process, submerged arc or tandem-MAG/GMAW welding is therefore a promising alternative, that also will be discussed.

Keywords: Laser hybrid welding, heavy section, structural steels, laser source, penetration

1. Background
Laser-arc hybrid welding is today a well-established process in the welding of structural steels, although thick part laser hybrid welding using penetration depth in excess of approximately 12 mm does continue to be a serious challenge. Industrially manufacturing of passenger ships may be the most outstanding example upon the success of laser hybrid welding with many yards especially in Europe using the processes in daily production.

While it originally was the CO2-laser source that dominated the structural steel area, the development of new generations of high power fiber and disc laser sources is now tilting this balance in their favour. In the case of the CO2 laser source it has for some time been realized that because og the beam-plasma
interaction the obtainable penetration depends very non-linear on the power level at hand, and the practical achievable weld thickness therefore is limited to below approximately 15 mm. Due to their emitted wavelength the fiber and disc lasers do not show the same intense beam-plasma interaction, which has given rise to the hope, that much larger penetrations may be achievable.

The rapidly growing wind turbine tower production area that in many countries like e.g. Denmark to a large extent constitutes the new heavy section industry does call for such larger penetrations. In this very heavy section welding industry welds in large structures as eg wind turbine towers and jackets involve welds well in excess of 25 mm in thickness, and the parts are therefore still fabricated by multi-pass arc welds, sometimes even in varying position as in the case of jackets. The vast majority of the industrial structural welding work in such areas is made by submerged arc welding (SAW) supplemented by manual arc welding.

In SAW high productivity may be achieved through the use of many parallel wires and arcs, but this does of course also often causes severe high heat input related problems with respect to mechanical and metallurgical properties as well as distortion. Figure 1 shows such a production facility and it is also interesting to notice that an operator is still required for each welding station since the fine tuning of the welding current as well as the final adjustment of the welding position transverse and parallel to the weld is performed manually.

![Figure 1. Typical Production situation in wind turbine power production.](image)

### 2. Limitations to penetration in laser hybrid welding

As long as laser welding has been performed the effects of the plasma has been discussed. In thin gauge CO₂ laser welding it is often supposed that the plasma is beneficial in that it increases the absorption of the laser light and thereby helps coupling the beam energy to the work piece. In thick plate welding it is on the other hand so that the plasma by absorbing part of the laser beam is detrimental to the penetration, and for this reason it is standard practice in pure laser welding to suppress the plasma on top of the work piece by a dedicated helium jet, but this is of course not possible in laser hybrid welding.

In laser hybrid welding it is well-known from the arc welding theory that the electric arc consists of gas and metal plasma, and from the laser welding theory
it must furthermore be expected that the keyhole is filled by metal plasma. An important point to notice is that the absorption of laser light by the plasma is extremely dependent on the wavelength of the laser light. To a first approximation the absorption coefficient is proportional to the square of the wavelength. As CO₂-laser light has a wavelength, which roughly is a factor of ten larger than the wavelength of Nd-YAG-, disc- and fibre-lasers, this has the important consequence, that plasma absorption is crucially more important in relation to CO₂-laser welding than in relation to welding with the other laser sources. Due to this effect fiber and disc lasers do not show the same intense beam-plasma interaction, which as mentioned has given rise to the hope, that much larger penetrations may be achievable. It should however be noted that a highly disturbing plume consisting of a mixture of hot non-ionized protection gas and metal vapour always seems to exist in and above the keyhole.

As experimental results have become available they do however seem to show that also these new laser source types under realistic heavy section manufacturing situations have a similar quite limited ability to reach the expected very large penetration depths. A recent comprehensive high power pure laser welding study using two disc lasers with a combined maximum power of 26kW [3] illustrates this. The two disc lasers sources used were a maximum 16kW, 8mm*mrad with a 0.2mm optical fiber and a 10kW, 12mm*mrad using a 0.3mm optical fiber, respectively, and no plume suppression jet was used. Figure 2 shows an example upon the achieved penetration at a welding speed of 1m/min in stainless steel.

It is possible to develop a simple analytical mathematical model based upon the absorption of the laser light by the metal plasma inside the keyhole ([1] and [2])). As the model originally was developed for CO₂ laser and CO₂ laser hybrid welding, it was at that time supposed that the mechanism controlling the energy absorbed in any depth inside the keyhole is plasma absorption, and that this energy subsequently is transferred to the cavity wall by a combination of heat transfer and secondary light emission and absorption. The consequential heat-flow is dissipated away in the work piece. As the direct plasma absorption for disc and fiber laser sources is approximately two orders of magnitudes smaller than for CO₂ laser sources the mechanism cannot be the same here, but on the other hand the experimental results points towards a total absorption of the same order of magnitude as for CO₂ laser sources. It is however well-known that the mentioned plume is very disturbing also for fiber and disc laser welding and the most obvious absorption mechanism is therefore angular scattering (as opposed to absorption) followed by absorption through multiple reflections.

The mentioned simple analytical model may therefore be applied also for fiber and disc laser heavy section welding. All models must be calibrated and doing so against the aforementioned experimental work by Katayama [3] the following two figures are obtained. Figure 3 shows the modelled penetration depth against the welding speed for various laser powers, while Figure 4 shows the penetration versus laser power for various welding speeds. The data from Katayama [3] used for calibration of the models two calibration parameters are also shown in the figures.
**Figure 2.** Achieved penetration as a function of laser power for 1m/min combined disc laser welding in stainless steel. Source Katayama et al [4]

**Figure 3.** Modelled penetration depth against the welding speed for various laser powers. The model is calibrated against the two data series from Katayama [3] shown on this and the next figure.
The experimental data series used for calibration consist of two series with variation in power and speed, respectively, and the model is seen to fit these point quite well. Although other pure laser welding results shows a somewhat deeper penetration (e.g. [4]) this may probably be explained by the use of an efficient plume suppressing jet, i.e. a technique that cannot be used in connection with hybrid welding. The model results may therefore be supposed to give a good indication of the potential of laser and laser hybrid high power welding. Thus it may be seen that also the new laser source types may under realistic heavy section manufacturing situations be supposed to show a quite limited ability to reach penetration depth in steels in excess of approximately 25 mm.

3. Future vision for automation in heavy section welding

For the welding of heavy sections in excess of 20-25mm when welding from one side and roughly 40mm when welding from two sides the only possibility seem to be multi-pass welding. In many applications as e.g. in wind turbine tower manufacturing the submerged arc welding process (SAW) is as mentioned earlier used extensively, but due to the high heat input this often causes severe problems with mechanical and metallurgical properties as well as with distortion. Multipass laser hybrid welding is possible, but does as discussed in e.g. [5] not extend the weldable thickness dramatically and may therefore only be considered as a solution with limited improvements in comparison with single-pass welding. In general therefore, substituting the submerged arc process with a root pass welded by laser-arc hybrid welding followed by filler passes using either the same process, submerged arc or tandem-MAG/GMA welding seems the most promising alternative to submerged arc welding. It must here be remembered that the weld groove area of the remaining groove varies with the square of the remaining thickness; - welding an e.g. 50 mm plate using a root face of e.g. 20 mm thus reduces the open groove area to approximately 36%. One could even
consider using the new robust and transportable fiber laser source for on-site erection welding thus substituting the troublesome and expensive flange connections.

Some concern have been raised about the mechanical properties of laser hybrid welded joint, but recent research, as e.g. reported in [2], has proved that the welds have very good mechanical properties in all aspects as e.g. hardness, toughness and fatigue. As an example Figure 5 show fatigue results for a hybrid weld in 20 mm structural steel showing results in excess of the FAT100 line.

**Figure 5**: Four-point bend fatigue testing in 20 mm RQT690 steel – from [2].

4. Conclusions

For many applications in wind turbine tower manufacturing the submerged arc welding process (SAW) is used and this often causes problems with mechanical and metallurgical properties as well as with distortion.

Laser hybrid welding offers due to the higher power density many advantages over traditional welding processes. The advantages include high speed seam welding, low distortion, single pass welding in large thickness, easy automation and positive effects on the working environment. For these reasons laser welding has also experienced a dramatic increase in use within structural applications and heavier sections in limited thickness. The ship building industry is leading in the introduction of high power laser and laser-hybrid welding and several European shipyards have now introduced the process.

When laser welding steels in greater thickness with high power lasers certain limitations to the obtainable penetration depth becomes apparent, and it is found that the obtainable penetration depends very non-linear on the power level at hand. The laser hybrid process does therefore even with the new high power
laser sources available not offer single pass welding from one respectively two sides in excess of roughly 20-25mm respectively 40mm. Substituting the submerged arc process with a root pass welded by laser-arc hybrid welding followed by fill-up passes using either SAW or GTA-welding is therefore the most promising alternative.

5. References


A FIRST ASSESSMENT OF LASER HYBRID WELDING OF 420 MPA STEEL FOR OFFSHORE STRUCTURE APPLICATION

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Abstract

For many years, laser hybrid welding has been used in various industries to increase productivity and reduce costs. One example is the adaption of the hybrid process in shipbuilding. The next natural step is to further develop the process for the oil and gas industry, where the welded joint properties requirements are more severe, and the ability to handle tolerance deviations is more critical. As a first attempt to develop hybrid laser process for the use in offshore structures, the present investigation addresses preliminary welding trails carried out with 15 kW fibre laser with appropriate gas metal arc welding equipment, using double Y joint geometry and 20 mm thick 420 MPa steel plates. The subsequent weld testing included both Charpy V notch impact and CTOD fracture mechanical testing at -30°C. The results indicate that the heat affected zone (HAZ) of the examined steel appeared with satisfactory Charpy and CTOD toughness (> 200 J, > 0.2 mm) while the weld metal had insufficient toughness (20-40 J, < 0.2 mm). With a better welding wire, designed for low temperature applications, it is reasonable to suggest that laser hybrid arc welding can be used for applications even below a temperature of -30°C.

Keywords: Laser hybrid welding, 420 MPa steel, impact properties, fracture toughness, microstructures

1. Introduction

In fabrication of steel structures, laser and laser hybrid welding has been increasingly taken into use due to the substantial development of laser technology. The introduction of fibre lasers provided much more flexibility in production, and the higher power supply (up to 50-60 kW) enhances the applicability for welding of thicker plates. This fact makes laser welding more flexible and suitable for robotization. In spite of this fact, laser hybrid welding has not
yet been adopted by the offshore oil and gas industry in spite of its use in shipbuilding, which represents some similarities with offshore structures. In fact, as short as two-year payback is reported in shipbuilding industry [1] (e.g., Defalco, 2007), which deems a huge economic potential to hybrid laser arc welding. However, there are also substantial differences, and the oil and gas industry has certainly not addressed automatic processes such as the case of high volume production with lot of generic products. Here, each individual structure or components have been regarded as unique. With more competition from far-east manufacture companies, productivity improvements must be sought to compensate for high man-hour costs. Another more material related difference issue is the toughness requirements for higher strength classes than in the shipbuilding case. Moreover, Charpy V notch toughness requirement increases with increasing strength. For steel with 420 MPa specified minimum yield strength (SMYS), the average Charpy V notch toughness should be 42 Joules at the selected test temperature of -40°C.

Therefore, the present investigation was initiated as a first attempt to examine laser hybrid welding of 420 MPa yield strength steel for offshore applications. The test programme addresses the Charpy V notch impact properties and the CTOD fracture toughness, both at -30°C. Finally, the residual stress evolution in welding and after post weld heat treatment (PWHT) has been assessed, together with numerical simulations. This work is reported elsewhere [2].

2. Materials and Experimental Procedure

2.1. Materials

The steel selected for the present study is typical offshore structure grade with 420 MPa specified minimum yield strength. Its chemical composition and mechanical properties are outlined in Tables 1 and 2, respectively. The steel is typically low carbon microalloyed grade supplied in 20 mm thickness. The yield and tensile strength were 479 and 586 MPa, respectively, which exceeds the SMYS requirement somewhat. The 1.0 mm diameter solid wire employed is alloyed with Ni (0.9%), which should be beneficial for the weld metal toughness.

<table>
<thead>
<tr>
<th>Material</th>
<th>C</th>
<th>Si</th>
<th>Mn</th>
<th>P</th>
<th>S</th>
<th>Cr</th>
<th>Ni</th>
<th>Mo</th>
<th>Cu</th>
</tr>
</thead>
<tbody>
<tr>
<td>Plate*</td>
<td>0.11</td>
<td>0.49</td>
<td>1.58</td>
<td>110</td>
<td>6</td>
<td>0.04</td>
<td>0.05</td>
<td>0.02</td>
<td>0.04</td>
</tr>
<tr>
<td>Wire</td>
<td>0.09</td>
<td>0.6</td>
<td>1.2</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>0.9</td>
<td>-</td>
<td>-</td>
</tr>
</tbody>
</table>

* The plate also contains 0.027% Nb and 0.033% Al; \(^1\) In ppm

Table 2. Mechanical properties of the rolled plate.

<table>
<thead>
<tr>
<th>YS, MPa</th>
<th>UTS, MPa</th>
<th>Charpy V, -60°C, J</th>
</tr>
</thead>
<tbody>
<tr>
<td>479</td>
<td>589</td>
<td>99-101-85</td>
</tr>
</tbody>
</table>

2.2. Welding

A 15 kW fibre laser was used together with standard power source for gas metal arc welding (GMAW), with the arc as lead. The set-up is illustrated by the photo contained in Fig.1. The welding parameters are outlined in Table 3, indicating low heat input for both the laser and
the MIG part, giving a total heat input of 1.9 kJ/mm. The parameters that remained unchanged are summarized in Table 4, indicating a laser focal point set to ~5 mm. The wire feeding rate was 55 mm/s, and the Mison 8 (92% Ar – 8% CO₂) shielding gas flow rate was set to 20 l/min. The laser hybrid welds were deposited on both sides of double Y-groove, Fig.2.

**Fig.1.** Hybrid laser MIG set-up, overview (left) and close-up (right).

**Fig.2.** Weld bevel.

**Table 1.** Laser welding parameters.

<table>
<thead>
<tr>
<th>Laser parameters</th>
<th>GMAW parameters</th>
<th>Total heat input, kJ/mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>Power, kW</td>
<td>Speed, mm/s</td>
<td>Heat input, kJ/mm</td>
</tr>
<tr>
<td>7.5</td>
<td>8.3</td>
<td>0.9</td>
</tr>
</tbody>
</table>
Table 4. Parameters that were kept constant.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Focal length</td>
<td>300 mm</td>
</tr>
<tr>
<td>Focal plane position</td>
<td>-5 mm</td>
</tr>
<tr>
<td>Laser inclination angle</td>
<td>7 degrees, trusting</td>
</tr>
<tr>
<td>Laser/MAG position</td>
<td>Leading MAG</td>
</tr>
<tr>
<td>Torch angle</td>
<td>75 degrees</td>
</tr>
<tr>
<td>Wire stickout</td>
<td>16 mm</td>
</tr>
<tr>
<td>Offset wire-laser</td>
<td>7 mm</td>
</tr>
</tbody>
</table>

2.3. Testing and characterization

Charpy V notch (CVN) specimens of the standard dimensions 10mmx10mm cross section and 55 mm length testing were machined from the laser hybrid arc welded joints with an intended notch positioned in the fusion line (FL) and the weld metal. The samples were located with their mid-thickness in the plate $t/2$ position, with the notch and thus the fracture parallel with the welding direction. Testing was performed at $-30^\circ C$. Three parallels were included.

Fracture toughness testing was performed in terms of SENB05 (Single Edge Notched Bending $Bx B$ type with crack depth of $a/t=0.5$). Both fusion line (FL) and weld metal (WM) were included. These were surface cracks with the fatigue pre-crack located in mid-thickness of the plate, which means that the fracture started in the laser part of the weld. During testing, the specimens were instrumented with clip gauges mounted at above the surface of the specimen. The reading from the clip gauges were used to extract CMOD and CTOD from the experimental results. In addition to the clip gauge measurements, the loading was recorded during the tests, carried out in displacement control, using standard setup in a 500 kN Instron servo hydraulic testing machine. The test temperature was $-30^\circ C$, as obtained through testing the specimens submerged in cooled liquor. The temperature was monitored by means of two thermocouples mounted on each side of the specimen. The test temperature was stabilized for 10 minutes prior to testing. The CTOD was calculated in agreement with BS7448 [3].

The metallographic examination included the following: (i) Macroscopic examination of the weld; (ii) Hardness measurements with 5 kg load (HV3); (iii) Weld metal and HAZ microstructure characterization; and (iv) Verification of notch positioning on selected Charpy V and CTOD samples. These specimens were subjected to standard metallographic techniques, including grinding, polishing and etching.

3. Results and discussion

3.1. Preliminary welding trials

Various joint types were preliminary tested, but the double Y-groove was considered to be the most production friendly one. However, these early welding trials gave some hot cracking problems, as evidenced by Fig.3 and 4. As shown by the micrographs, these cracks are typically located in the centreline of the double sided symmetric welds. Their location is at the solidification front of the laser part of the weld, and is often related to microsegregation of solute and impurity elements which then may not withstand the local transient tensile thermal stresses which arise on cooling, Fig.5. Solidification cracks in weld metals are often related to excessive height-to-width ratio. Moreover, the geometry of the weld bevel may also be
important, as will the welding speed. However, these factors are discussed elsewhere [4].

![Fig.3. Laser weld metal solidification cracking (crack length of 1.6 mm).](image3)

![Fig.4. Laser weld metal solidification cracking; polished (left) and etched (right).](image4)

![Fig.5. Laser hybrid GMAW weld. The dashed white line indicates the line for the last part to solidify with potential microsegregation of solute and impurity elements.](image5)
In parallel with the solidification cracking problem, the hardness of the weld was high, with maximum values of 375-385 HV10.

### 3.2. Charpy V notch toughness

In deposition of welds for toughness testing, the laser effect was reduced from 10-11 to 7.5 kW, and the welding speed was reduced from 33.3 to 8.3 mm/s. The associated weld macrograph is shown in Fig.6. It is evidenced that there is no solidification cracks present. Therefore, both Charpy V notch and CTOD fracture toughness were carried out (-30°C). The Charpy V results are shown in Fig.7. The results are totally different for the weld metal and the fusion line. The weld metal impact properties are low with two single values below 30 J. By contrast, the fusion line notch toughness was high with all values between 200 and 250 J. These differences are further discussed in the metallographic section below. There are very few relevant data available in literature for comparison with the present study. Results from welding of an X60 pipeline, which represents the same strength class as the 420 MPa plate, indicated Charpy V values between about 40 and 80 J at -30°C [5], which is far below results of the present study. However, the comparison should be done with care, since the authors did not report test details such as notch position or notch orientation.

![Weld macrograph](image)

**Fig.6.** Weld macrograph.

![Charpy V notch toughness](image)

**Fig.7.** Charpy V notch toughness of laser-GMA weld (-40°C).
3.3. Fracture toughness

The CTOD results (-30°C) are plotted in Fig.8. As for the Charpy V test, the CTOD level was highest for the fusion line with scatter from 0.28 to 0.63 mm. These data are much higher than previous published results from welding of ship steel with 355 MPa yield strength, which was tested at -20°C [6]. The weld metal gave CTOD level below 0.2 mm with the highest single value of 0.18 mm.

![CTOD fracture toughness of laser-GMA weld (-30°C).](image)

3.4. Verification of notch positions

Few Charpy and CTOD samples were selected for a closer examination of the notch and fatigue pre-crack positions. It is evident from Fig.9 that the Charpy V notch is located in the halfway from the fusion line towards the base metal, which is the outer area of the coarse grained region close to the fine grained HAZ. The distance from the fusion line to the notch varied, but was typically between 0.2 and 0.4 mm.

![Location of Charpy V notch.](image)
The similar examination of CTOD samples revealed that although the notch was quite accurately hitting the fusion line, the fatigue pre-crack tends to deflect slightly towards the fine grained HAZ, as shown in Fig.10.

Thus, both Figs.9 and 10 imply that the Charpy V notch and the CTOD fatigue pre-crack have been positioned slightly outside the desired area. Concerning the fatigue pre-crack, the accurate positioning is difficult because the crack growth may be influenced by the local microstructures and their strength, which in fact, may vary from grain to grain due to the inhomogeneous nature of the heat affected zone. It is reasonable to suggest that the measured fusion line toughness is somewhat high due to the finer austenite grain size compared with the region closer to the fusion line.

The weld metal samples do not have the same challenge in notch and fatigue pre-crack positioning.

![Fig.10. Location of CTOD surface notch and fatigue pre-crack.](image)

3.5. Microstructure characterization

The weld metal and HAZ microstructures of both the GMA and laser weld parts were examined. The micrographs are contained in Figs.11-12. In all cases, the microstructure consists of a mixture of martensite and bainite, but the volume fraction of the two constituents may vary. In addition, there are numerous single needles or laths resembling acicular ferrite. However, these are probably not nucleated on non-metallic inclusions, and may thus not be as positive for the toughness. Finally, The HAZ of the laser weld microstructure in Fig.12 contains a saw tooth-like ferrite nucleated at prior austenite grain boundaries with limited growth length, mostly below 10\(\mu\)m long. This microconstituent is probably Widmanstätten ferrite, which growth is prevented by the cooling rate and the martensite growth rate. The prior austenite grain size in the coarse grained HAZ falls roughly between 70 and 80 \(\mu\)m, with no noticeable difference between the GMAW and laser HAZ.

The microstructure observations are in agreement with the measured hardness levels. The maximum HV values are outlined in Table 5, indicating that the highest values were found in the GMAW part, with hardness of 355 and 344 in the HAZ and weld metal, respectively. The corresponding values for the laser part were 313 (HAZ) and 316 (weld metal).
Table 5. Maximum hardness in different positions.

<table>
<thead>
<tr>
<th>Base metal</th>
<th>GMAW HAZ</th>
<th>GMAW WM</th>
<th>Laser HAZ</th>
<th>Laser WM</th>
</tr>
</thead>
<tbody>
<tr>
<td>Base metal</td>
<td>GMAW</td>
<td></td>
<td>Laser</td>
<td></td>
</tr>
<tr>
<td>HAZ</td>
<td>355</td>
<td>355</td>
<td>344</td>
<td>344</td>
</tr>
<tr>
<td>WM</td>
<td>344</td>
<td>344</td>
<td>313</td>
<td>313</td>
</tr>
</tbody>
</table>

3.6. Fracture surfaces

In the verification of notch and fatigue pre-crack locations for CTOD samples with weld metal notch, it was found that some porosity existed. These are illustrated by the fracture surface overview contained in Fig.13. It is not known how this porosity eventually influences the toughness properties in the present investigation. Such pores were not found on the fracture surfaces of Charpy V notch sample.

For the CTOD samples, there was a major difference between the fusion line and weld metal notched fracture surfaces. The scanning electron microscopy (SEM) fractographs are shown in Fig.14. It is seen that the fusion line sample, with a CTOD value of 0.28 mm, contained a ductile region immediately in front of the fatigue crack tip. The weld metal specimen had brittle fracture initiation close to the fatigue crack tip. However, there is
substantial topography in the surface indicating that there exists certain crack arrest in the actual microstructure, which is also in agreement with the CTOD value of 0.15 mm.

![Fig.13. Weld metal porosity (encircled).](image)

**Fig.13.** Weld metal porosity (encircled).

![Fig.14. SEM fractographs from CTOD samples with (left) fusion line notch and (right) weld metal notch (initiation area encircled).](image)

**Fig.14.** SEM fractographs from CTOD samples with (left) fusion line notch and (right) weld metal notch (initiation area encircled).

### 3.7. Practical implications

The present investigation was carried out without extensive optimization of welding parameters. Moreover, the steel plate and the welding wire were taken from an in-house stock. Therefore, it is reasonable to suggest that there is huge potential for enhancement of the low temperature toughness. The main challenge seems to be to provide sufficient amount of deposit into the laser part without having too much fused base metal. With excessive base metal melting, the laser part will not have the desired volume fraction of nonmetallic inclusions to promote acicular ferrite formation; instead prior austenite grain boundaries will be controlling the solid state phase transformation, resulting in martensite and bainite mixtures. The resulting HAZ and weld metal hardness levels became too high, which alone should be possible to reduce through e.g. post weld heat treatment (PWHT). However, PWHT may in practice cause an unacceptable cost increase, and hence, counteract the inherent productivity of laser hybrid welding. Therefore, further work should be performed to achieve welds with lower hardness in combination with better low temperature toughness.
Finally, the weld bevel employed in this study may not be the easiest choice from a testing viewpoint. In qualification of welding procedures, one of the sidewalls is usually vertical to be able to carry out fusion line toughness testing (straight vertical fusion line), and through thickness notch is frequently applied to raise the probability of hitting eventual local brittle zones present along the weld. The double Y-groove has also vertical sidewalls in the mid-thickness area, but it was difficult to position the notch in the desired area since it is quite narrow. In addition, the width of the potential local brittle zones is limited in laser hybrid welding. This may, in turn, influence the fatigue pre-crack position in CTOD testing due to the local differences in yield strength. Further studies of the process are required with respect to fracture mechanics testing, and the verification of notch locations after testing is important to omit samples which failed to hit the desired area.

4. Conclusions

The present investigation was carried out with the objective to achieve a first assessment of laser hybrid welding for the oil and gas industry. The following conclusion are drawn:

- Preliminary welding trials revealed some hot cracking problems with the selected weld bevel.
- The hardness level both in the HAZ and the weld metal was too high, with non-desirable microstructures formed, particularly in the weld metal.
- The Charpy V notch toughness (-30°C) of the weld metal was low (~20 J).
- The impact properties of the HAZ/fusion line (-30°C) was very high (> 200 J).
- The CTOD fracture toughness (-30°C) was high for the fusion line/HAZ, i.e., between 0.28 and 0.63 mm.
- For the weld metal, the CTOD level (-30°C) falls in the range from 0.13 to 0.18 mm.

5. Acknowledgements

The authors wish to thank the Research Council of Norway for funding through the Petromaks Programme, Contract No.187389/S30. The financial support from ENI, Statoil, Total, Scana Steel Stavanger, JFE Steel Corporation, Nippon Steel Corporation, Brück Pipeconnections, Miras Grotnes, Bredero Shaw, Trelleborg, GE Oil and Gas, Aker Solutions and Technip are also acknowledged. Finally, the excellent effort carried out by Mr Tore Andre Kristensen and Mr Andrew Marson in testing and Mrs Synnøve Åldstedt in metallography is deeply appreciated.

6. References

[3] British Standards Institution:


RESIDUAL STRESSES OF HYBRID LASER-ARC WELDING FOR ARCTIC APPLICATION

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¹ SINTEF Materials and Chemistry, Trondheim, Norway
² Norwegian University of Science and Technology, Trondheim, Norway
³ Luleå University of Technology, Luleå, Sweden

Abstract

Hybrid laser-arc welding has been widely used due to its high productivity. Since efficient and reliable welding technology is one of the main challenges for exploring oil and gas in the arctic region, the present investigation was initiated to examine the residual stress level and distribution in hybrid laser-gas metal arc welding of 20 mm thick plates of 420 MPa yield strength steel. With design temperature approaching -60°C, it is important to consider influence of residual stresses, since they may reduce the maximum allowable weld defect size due to high risk of brittle fracture. Welding trials were carried out with a 15 kW fibre laser and appropriate arc welding equipment using a double Y joint geometry. Welding residual stresses were measured using the hole-drilling method. Tensile longitudinal residual stresses occurred in all locations, ranging from 250 MPa to 700 MPa. After post weld heat treatment at 600°C for two hours, these stresses were reduced to less than 100 MPa. Compressive transverse stresses were found in the heat affected zone, ranging from 150 to 200 MPa. Numerical simulation of hybrid laser-arc welding was performed using WeldsimS. The resulting simulated stress-field is compared with measured data.

Keywords: hybrid laser-arc welding, residual stresses, WeldsimS, hole-drilling

1 Introduction

Efficient and reliable welding technology is one of the main challenges for exploring oil and gas in the arctic region due to environmental and safety concerns. In an on-going project [1], hybrid laser-arc welding technique is considered as one of the welding solutions for arctic applications. Hybrid laser-arc welding has been widely used due to its high productivity, good weld joint quality, low heat input and thus low distortion. Residual stresses after welding cannot be avoided, however, which is particularly important to consider in low design temperature due to high risk of brittle fracture. Structural steels typically have good ductility in room temperature, thus the plastic deformation near a crack tip will override any pre-existing elastic residual stresses [2]. In the arctic region, design temperatures down to 60°C are being used, which may be below the ductile-brittle transition of the weld, HAZ and/or base material. With reduced ductility, the high residual stresses found after welding may lead to crack propagation in small defects that would otherwise be considered safe. It is therefore important to obtain accurate distribution of welding residual stresses in order to investigate their effect on the structural integrity.

In order to quantify residual stresses, a combination of numerical modelling and measurements is used in this study. The finite element (FE) solution provides detailed stress distribution throughout the volume, while a hole-drilling technique is used to check and verify
the FE solution. The comparisons of predicted and measured residual stresses are reported in this paper.

2 Experimental details

2.1 Materials

The steel selected for this study was an "in-house" 20 mm thick 420 MPa plate. The wire was a 1.0 mm LNM Ni1 with typical all weld metal yield and tensile strength of 480 and 580 MPa, respectively.

2.2 Welding

The length, width and thickness of welded plate are 600 mm, 200 mm and 20 mm, respectively. The hybrid laser-GMA welding was deposited on both sides of the Y-groove, as shown in Fig. 1.

![Fig. 1 Joint configuration for hybrid laser-arc welding](image1)

The hybrid laser-GMA welding set-up is shown in Fig. 2.

![Fig. 2 Hybrid laser-GMA welding set-up](image2)

The welding parameters are outlined in Table 1, indicating low heat input for both the laser and the MIG part, but adding up to around 1.5-2 kJ/mm. A laser beam power of 7.5 kW was used and the focal point was -5 mm. The wire feeding rate was 55 mm/s, and a Mison 8 (92% Ar – 8% CO2) shielding gas flow rate set to 20 l/min. The MIG-arc was operated in pulsed mode, transferring one drop per pulse. The given current is the pulse-high current.
Table 1: Welding parameters

<table>
<thead>
<tr>
<th>Laser parameters</th>
<th>GMAW parameters</th>
<th>Total heat input</th>
</tr>
</thead>
<tbody>
<tr>
<td>Power [kW]</td>
<td>Current [A]</td>
<td>Heat input [kJ/mm]</td>
</tr>
<tr>
<td>Speed [mm/s]</td>
<td>Voltage [V]</td>
<td>Speed [mm/s]</td>
</tr>
<tr>
<td>Heat input [kJ/mm]</td>
<td>Heat input [kJ/mm]</td>
<td></td>
</tr>
<tr>
<td>7.5</td>
<td>312</td>
<td>2.3</td>
</tr>
<tr>
<td>8.3</td>
<td>26</td>
<td>0.9</td>
</tr>
<tr>
<td>0.9</td>
<td>8.3</td>
<td>1.0</td>
</tr>
<tr>
<td></td>
<td></td>
<td>1.9</td>
</tr>
</tbody>
</table>

2.3 Residual stress measurements

Stresses were measured using SINT MTS 3000, which permits automated acquisition using the incremental hole drilling method. Strain relief is measured using strain gauge rosettes centered around the hole, from which residual stresses are calculated using the accompanying software. The method adheres to the non-uniform procedure described in ASTM E837 [3]. A three-wire connection was used to each strain gauge, and a Spider-8 digital amplifier was used for data acquisition. HBM type B (1-RY61-1.5/120K) strain gauges were used with a strain gauge radius of 2.55 mm. An inverted cone carbide end-mill with a diameter of 1.8 mm was used, and the holes were drilled to 2 mm depth. The majority of tests were conducted using a non-linear increase in drilling increments, with smaller increments near the surface. The final three measurements were made in 80 equally spaced increments. Stresses are calculated down to a depth of 1 mm. Measurements were corrected for eccentricity after the hole was drilled. Stress calculations are done according to the integral method [4] with correction for hole/rosette eccentricity using the Restan software. Elastic material behavior is assumed in this method, thus the accuracy decreases as Mises stress levels approach the yield stress of the material.

The measurement positions are shown in Fig. 3. The measurements on the weld center line and 10 mm from the weld center were repeated to check for variability along the length of the weld. Two measurements were repeated again after heat treatment, at 10 mm from the center. Two measurements were made on the back side of the plate in the center of the weld, one before heat treatment and one after.

Fig. 3 Positions for residual stress measurements on the front side of the plate. Position 11 and 12 were measured after heat treatment.
3 Numerical details

WeldsimS [5] is purpose built for the requirement of welding simulations, which can handle complex welding phenomena, for instance, moving heat source, multi-pass welding, phase transformations and microstructure evolution.

A combined model of a cylindrical volume heat source and a double ellipsoidal heat source was used to simulate hybrid laser-arc welding. When the heat source approaches, the elements in the weld bead domain become activated, and the density factor that is initially zero is increased simultaneously as heat is added to keep the temperature above the liquid temperature.

Low alloy ferritic steel consists of austenite at the high peak temperatures involved in welding, and transforms to ferrite, pearlite, bainite and/or martensite depending on the cooling rates involved. In welding simulation, thermo-physical and constitutive relations are assigned for each phase, and a law of mixture is used to evaluate the properties in a volume element [6]. The flow stress of each phase is computed based on Ludwik-Hollomon equation, which has a normalized form as follows:

\[
\sigma = F(T) \left[ \left( \phi_0(T) + \phi \right) / \phi_0(T) \right]^{n(T)} \left( \tilde{\varepsilon}_p / \tilde{\varepsilon}_0 \right)^m(T)
\]

(1)

where \(F, m, n\) and \(\phi_0\) are temperature dependent functions, \(\tilde{\varepsilon}_0\) is a reference strain rate and \(\phi\) is a work hardening parameter that corresponds to the integrated plastic strain. A CCT diagram [7] approach has been used to simulate the phase-transformation. Temperature dependent material properties, e.g. Young’s modulus, yield stress, specific heat capacity as well as heat conductivity has been applied as input.

Full plate was modeled, and the model was meshed by eight-node 3D continuum element C3D8. The model has 123120 elements. Fine mesh was created in weld and heat affected zone to capture the microstructure evolution and temperature development. The regions where residual stresses were measured by experiments were meshed using fine mesh as well. The global and local mesh of the model is shown in Fig. 4.

Fig. 4 Finite element mesh of the model
4 Results and discussion

4.1 Weld bead profile

Fig. 5 Comparison of the weld profile, (a) numerical prediction and (b) macrograph. The grey zone in numerical prediction represents the peak temperature over 1450°C, which illustrates the fusion zone.

It can be seen that WeldsimS simulation captured the real weld bead profile well. However, the dimension of the predicted weld bead is larger than the measured result. In simulation, the loss of input heat was not considered, i.e. the arc efficiency was 100%. There is no measurement of arc efficiency or relatively reasonable estimation of arc efficiency for hybrid laser-arc welding in this study.

4.2 Residual stresses

High gradient local heating induces residual stresses through the combined effect of thermal strains, phase transformations and variation of material properties with temperature. High tensile residual stresses can be produced by welding in weld and heat affected zone and balanced by compressive residual stresses in base metal. It is known that tensile residual stresses have detrimental effect on structural integrity. It is thus very interesting to accurately map the residual stress field. In this study, both numerical and experimental investigations of hybrid laser-arc welding residual stresses have been carried out.

Post weld heat treatment (PWHT) has not been taken into account in numerical simulations. Therefore, only residual stresses at top surface of as-weld plate were compared between numerical and experimental results. As indicated in Fig. 3, hole-drilling measurements at position 1, 3, 4, 5, 7 (green) yield valid results. Position 11 and 12 were measured after the PWHT. Measured residual stresses are showed at 4 different depth of drilling hole, i.e. 0.05 mm, 0.15 mm, 0.25 mm and 0.45 mm. It should be noted that predicted residual stresses showed in Fig. 6 are taken from the transverse path at longitudinal centre of the plate. A weld bead is modelled, thus the change in surface normal will give rise to variations in calculated coordinate stresses. For numerical simulations, distribution of residual stresses does not change much along the longitudinal direction where hole-drilling measurements were carried out. Therefore, only one distribution of residual stresses at longitudinal centre is compared with hole-drilling measurements.
Fig. 6 Comparison of predicted and measured residual stresses
It can be seen that tensile longitudinal residual stresses are present in weld and heat affected zone through both FE simulation and hole-drilling measurements. Hole-drilling measurements vary with depth of holes. It seems that measurement reading at start of drilling may significantly underestimate the magnitude of residual stresses. Numerical simulation can well predict the longitudinal residual stresses compared to measured results. However, for transverse residual stresses, FE simulation predicts tensile residual stresses at weld and heat affected zone while hole-drilling measurements indicate compressive residual stresses at same region. It seems that depth of drilling hole has significant effect on the results. For instance, at depth d=0.45 mm, numerical prediction fits the measured magnitude of residual stresses very well at the centre of the weld.

In general, numerical modelling results can somewhat fit limited hole-drilling measurements. The deviation between numerical and experimental results might be caused by both numerical and experimental aspects. More accurate material data and constitutive law may improve the results. Meanwhile, more accurate and stable hole-drilling measurements should be carried out. **Fig. 7** shows the predicted through-thickness residual stresses.

![Fig. 7 Distribution of through-thickness residual stresses obtained by numerical simulation](image)

It can be seen that compressive transverse residual stresses are predicted at the middle surface section of the welded plate, while tensile transverse residual stresses are present near surfaces. However, tensile longitudinal residual stresses are mainly predicted through the thickness of plate while compressive residual stresses are present near the lower surface.

Distribution of residual stresses through the hole-drilling depth is compared between numerical simulation and measurement. Results measured at two locations are compared, i.e. Position 1 at weld centre and Position 5 at fusion line. **Fig. 8** shows the comparison of predicted and measured residual stresses.
Fig. 8 Comparison of distribution of residual stresses through hole-drilling depth, (a) Position 1 and (b) Position 5. "FE" represents "Finite Element" simulation; "HD" denotes "Hole-drilling" method.
It can be seen that FE simulation does not fit the measurement well. FE simulation overestimated the magnitude of transverse residual stresses while underestimated longitudinal residual stresses through the depth of hole.

5 Conclusions

In this study, hybrid laser-arc welding residual stresses are investigated through numerical modelling and hole-drilling measurement. Tensile longitudinal residual stresses present in weld and HAZ, ranging from 250 MPa to 750 MPa. After post weld heat treatment at 600°C for two hours, these stresses were reduced to less than 100 MPa. Compressive transverse stresses were measured in the heat affected zone, ranging from 150 to 200 MPa. Numerical modelling shows deviation to hole-drilling measurement, especially for transverse residual stresses. Accuracy of numerical modelling and hole-drilling method should be further investigated.

6 Acknowledgements

The authors wish to thank the Research Council of Norway for funding through the Petromaks Programme, Contract No.187389/S30. The financial support from ENI, Statoil, Total, Scana Steel Stavanger, JFE Steel, Nippon Steel Corporation, Brück Pipeconnections, Miras Grotnes, Bredero Shaw, Trelleborg, GE Oil and Gas, Aker Solutions and Technip are also acknowledged.

7 References

LASER-GMA HYBRID WELDING OF HIGH STRENGTH STEEL GRADES WITH THE YIELD STRENGTH OF 700 MPA

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Abstract

In recent years the Finnish steel making company Ruukki Metals Oy has developed the new production route for steel plates called a thermo-mechanical hot rolling. The direct quench process integrated to the hot rolling is used for the manufacturing of the steel grade Optim 700 QL (S690QL EN 10025-6). At the same time, the improvement of the toughness and formability of thermo-mechanically processed strip steel grade Optim 700MC Plus (S700MC EN 10149-2) has opened up possibilities for more effective utilization of higher strength steels in many light-weight constructions, such as in the beams of the commercial vehicles, the girders of bridges. The aim of the study was to compare the weldability of the 10 mm thick direct quenched and tempered steel Optim 700 QL and thermo-mechanically processed steel Optim 700MC Plus in the laser-GMA hybrid welding. The welding power sources used were a 12 kW disc laser by Trumpf and a Time 5000 Digital MAG power source of Fronius. The quality and properties of the welded joints were carried out using both destructive and non-destructive testing in accordance with EN-ISO 15614-1 and SFS-EN ISO 15614-11. The destructive testing of the joints consisted of tensile, impact and bend tests, as well as hardness measurements and metallographic investigations. The joints fulfilled the strength properties specified for the base plate and the impact toughness transition temperature $T_{27J}$ was $-40$ °C or lower throughout the weld metal and HAZ.

Keywords: Optim 700QL steel, Optim 700MC Plus steel, disc laser-GMA hybrid welding, butt welds, mechanical properties, hardness, metallography
1 Introduction

The high strength steel grades with the yield strength of 700 MPa are commonly used for light-weight constructions, especially for the structural members of mobile equipment, where the weight saving and the increased load bearing capacity are central requirements for the cost efficiency and the environmental protection. Typical applications are load bearing structural members for commercial vehicles, machines and other lifting and transport equipment.

After including of 700 MPa steels in the European design and fabrication standards Eurocode 3 [1] and EN 1090-2 [2] the use of these steels has increased. In future, the high-strength steel grades with a yield strength of 700 MPa will open up the innovative and competitive applications for ship, offshore constructions and even pressure vessel industry.

In recent years, laser and laser-GMA hybrid processes have gradually become more widely applied and opened up possibilities for more effective welding. Laser and laser-GMA hybrid welding were expected to prevent a distortion of the welded structures reducing a need of levelling, due to the narrow weld metal and HAZ resulting from a very low heat input. With the use welding process like laser and laser-GMA hybrid is possible to achieve matching weld metal compared to the base material even with the low alloying welding wires without excessive softening of the HAZ in the high strength steels [3].

In this study the weldability of 10 mm thick direct quenched and tempered steel plate Optim 700 QL and thermo-mechanically processed strip steel Optim 700MC Plus were compared welding in the laser-GMA hybrid process.

2 Comparison of Optim 700 QL and Optim 700MC Plus steels

2.1 Applications of 700 MPa steels

In recent years the use of 700 MPa steels has increased, especially for lightweight constructions, such as the structural members of mobile equipment, in order to reduce the weight and the fabrication costs as well as to contribute the performance and the environmental protection. Typical applications include load bearing structural members for commercial vehicles, machines and other lifting and transport equipment (Fig. 1.) [7].

Fig. 1. Example of the application of 700 MPa steels [7].

2.2 Manufacturing routes of Optim 700 QL and Optim 700MC Plus steels

Ruukki Metals Oy has developed the new plate production route called thermo-mechanical hot rolling integrated with the direct quenching and tempering for the manufacturing steel grade Optim 700 QL (S690QL EN 10025-6) [4,5]. At the same time, the development of the
thermo-mechanical strip rolling followed by an accelerated cooling has improved the toughness and the formability of strip rolled steel grade Optim 700MC Plus (S700MC EN 10149-2) [6] and given a more economical alternative for steel grade Optim 700 QL. By using thermo-mechanical rolling, finer microstructures with improved toughness are also possible. Combining rolling and quenching into a single process also has logistical advantages over conventional reheating and quenching helping to make delivery times shorter and more precise (Fig. 2.) [4].

2.3 Alloying and weldability differences of Optim 700 QL and Optim 700MC Plus steels

The most commonly used welding method for Optim 700 QL and Optim 700MC Plus steels is the gas metal arc welding (GMA) either with solid or flux-cored wires. Nowadays, laser and laser-GMA hybrid welding and modern pulsed GMA welding with a very low heat input had gradually become more widely applied and opened up possibilities for more effective welding. [7].

Since the strip rolled steel Optim 700MC Plus is not tempered, it has a lower level of carbon and the alloying elements than direct or conventional quenched and tempered steels with the same yield strengths. This difference is shown in the hardness profiles across welds: Optim 700 QL steel has typically high hardening of the HAZ, whereas Optim 700MC Plus steel has a little softening of the HAZ compared with the base plate. These differences are apparent in the hardness profiles in (Fig. 3). Preheating of Optim 700 MC Plus steel is not normally required for the small plate thicknesses (≤ 12 mm) and low carbon equivalent provided that the hydrogen content of the welding consumables is kept very low.
Hardness profiles of GMA butt joints, $E=0.5 \text{ kJ/mm}$

![Hardness profiles of GMA butt joints](image)

Optim 700 QL and Optim 700MC Plus steels. Heat input was 0.5 kJ/mm and plate thickness 10 mm. The welding wire was Böhler X70-IG.

Fig. 3. Vickers hardness profiles of the MAG welded butt joints in Optim 700 QL and Optim 700MC Plus steels. Heat input was 0.5 kJ/mm and plate thickness 10 mm. The welding wire was Böhler X70-IG.

Hardening of the HAZ is the main limiting factor of the minimum heat input in welding of Optim 700 QL steel, whereas for the low carbon Optim 700MC Plus steel softening of the HAZ limits the maximum heat input. In generally, the increased heat input leads to the decreased yield and tensile strengths and impact toughness due to grain coarsening. Softening of the HAZ can be reduced by decreasing the heat input, e.g. by increasing the welding speed. This is possible with the low heat input welding methods such as laser and laser-GMA hybrid welding and pulsed GMA welding [8].

The impact toughness of the heat affected zones of Optim 700 QL and Optim 700MC Plus steels fulfils typically the impact energy requirement of 34 J/cm² at -40 °C, which corresponds to the 27 J for a full size 10 x 10 mm standard test specimen. This realizes even with the laser-GMA hybrid welds welded with very short cooling times [7,8].

### 3 Experimental set-up

#### 3.1 Test plate and filler materials

The test materials comprised the steels Optim 700 QL and Optim 700MC Plus with a thickness of 10 mm. The joint preparation was the V-joint with a 10° groove angle. The depth of the root face was 5 mm in the joints of Optim 700 QL steel and 6 mm in the joints of Optim 700MC Plus steel. The size of the test pieces was 150 x 1000 mm (300 x 1000 mm as welded). The solid filler wire of Böhler X70-IG (EN ISO 16834-G 69 5 M Mn3Mi1CrMo) with a diameter of 1 mm. The chemical composition and the mechanical properties of the base material (specified) and the filler materials (typical) are given in Tables 1 and 2.
Table 1. Chemical composition of the base plates (max. specified) and filler wire (typical) (in wt.%).

<table>
<thead>
<tr>
<th>Material</th>
<th>Optim 700 OL</th>
<th>Optim 700MC Plus</th>
<th>Böhler X70-IG a</th>
</tr>
</thead>
<tbody>
<tr>
<td>t [mm]</td>
<td>10</td>
<td>10</td>
<td>Ø 1.0</td>
</tr>
<tr>
<td>C</td>
<td>0.20</td>
<td>0.10</td>
<td>0.1</td>
</tr>
<tr>
<td>Si</td>
<td>0.80</td>
<td>0.50</td>
<td>0.6</td>
</tr>
<tr>
<td>Mn</td>
<td>1.70</td>
<td>2.10</td>
<td>1.6</td>
</tr>
<tr>
<td>P</td>
<td>0.020</td>
<td>0.020</td>
<td></td>
</tr>
<tr>
<td>S</td>
<td>0.010</td>
<td>0.010</td>
<td></td>
</tr>
<tr>
<td>Al</td>
<td>0.015</td>
<td></td>
<td></td>
</tr>
<tr>
<td>B</td>
<td>0.005</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Cr</td>
<td>1.50</td>
<td></td>
<td>0.25</td>
</tr>
<tr>
<td>Cu</td>
<td>0.50</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Ni</td>
<td>1.3</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Mo</td>
<td>0.70</td>
<td></td>
<td>0.25</td>
</tr>
<tr>
<td>V</td>
<td>0.1</td>
<td></td>
<td>0.1</td>
</tr>
<tr>
<td>CEV</td>
<td>0.42</td>
<td>0.40</td>
<td></td>
</tr>
</tbody>
</table>

a Filler wire \( CEV = C + \frac{Mn}{6} + \frac{(Cr+Mo+V)}{5} + \frac{(Cu+Ni)}{15} \)

Table 2. Mechanical properties of the steels (specified) and filler wire (typical).

<table>
<thead>
<tr>
<th>Material/ Filler wire</th>
<th>t [mm]</th>
<th>( R_{eH/p0.2} ) [N/mm²]</th>
<th>( R_m ) [N/mm²]</th>
<th>A5 [%]</th>
<th>Impact energy Average min. [J]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Optim 700 QL</td>
<td>10</td>
<td>690</td>
<td>770-940</td>
<td>14</td>
<td>30 ^ad</td>
</tr>
<tr>
<td>Optim 700MC Plus</td>
<td>10</td>
<td>680</td>
<td>750-940</td>
<td>13</td>
<td>40 ^bd</td>
</tr>
<tr>
<td>Böhler X70-IG</td>
<td>Ø 1</td>
<td>690</td>
<td>790</td>
<td>16</td>
<td>47 ^c</td>
</tr>
</tbody>
</table>

Test temperatures: a=-40 °C, b=-60 °C, c=-50 °C, d=minimum

3.2 Welding of the test pieces

The laser used in the laser-GMA hybrid welding experiments was a 12 kW disc laser by Trumpf equipped with a 400 μm feeding fibre and optics with a 200 mm focal length of collimation and a 300 mm focal length for focusing. The diameter of the laser beam used was 0.6 mm. The arc power source used was Fronius Time 5000 Digital. A leading GMA torch was used with a slope angle of 25° and the distance between the laser beam and the end of the filler wire was 1.5 mm (Fig. 4). The focus position was -7 mm for Optim 700 QL steel and -6 mm (below from the test plate surface) for Optim 700MC Plus steel. The shielding gas used was a gas mixture with a content of 92% argon and 8% CO₂ and the gas flow of 25 l/min was used. The movement of the welding test was performed with a Kuka KR 30 HA-C industrial robot. The test pieces were clamped to the welding fixture and the joints were tack welded manually by the GTAW without filler material before the laser-GMA hybrid welding. The main welding parameters are presented in Table 3.
Fig. 4. The GMA welding torch and laser optics (a) and welding in flat position (b).

Table 3. The main welding parameters.

<table>
<thead>
<tr>
<th>Material</th>
<th>Optim 700 QL</th>
<th>Optim 700MC Plus</th>
</tr>
</thead>
<tbody>
<tr>
<td>Laser Power [W]</td>
<td>8700</td>
<td>11000</td>
</tr>
<tr>
<td>MAG Current [A]</td>
<td>238</td>
<td>241</td>
</tr>
<tr>
<td>MAG Voltage [V]</td>
<td>26.5</td>
<td>28.0</td>
</tr>
<tr>
<td>Travel speed [m/min]</td>
<td>1.8</td>
<td>2.28</td>
</tr>
<tr>
<td>Filler wire feed rate [m/min]</td>
<td>14</td>
<td>16</td>
</tr>
<tr>
<td>Welding Energy [kJ/mm]</td>
<td>0.50</td>
<td>0.47</td>
</tr>
</tbody>
</table>

3.3 Testing of the welded pieces

The welds were inspected visually and radiographically according to SFS-EN 970 [9] using the approval criteria of welds according to EN ISO 13919-1, 1996 [10] and SFS-EN ISO 5817 [11] (B = stringent, C = intermediate and D = moderate) for these laser-GMA hybrid welds.

Mechanical testing of the joints was carried out according to ISO EN 15614-1 [12] and SFS-EN ISO 15614-11 [13]. Testing procedure consisted of tensile, bending and Charpy V impact tests, hardness measurements and metallography.

4 Test results

4.1 Non-destructive and destructive tests

The results of radiographic inspection showed that the weld had gas pores (code 2011) distributed uniformly (code 2012) and clustered (code 2013). In addition, a lack of penetration (code 402) and an incompletely excessive penetration (code 511) was found. However, the evaluation showed that the weld of Optim 700 QL steel fulfilled class C and that of Optim 700MC Plus steel class B.

The tensile properties of the butt joints are given in Table 4.
Table 4. Tensile test results from the laser MAG hybrid welded butt joints

<table>
<thead>
<tr>
<th>Steel grade</th>
<th>( R_{eH} ) [N/mm²]</th>
<th>( R_m ) [N/mm²]</th>
<th>( A_5 ) [%]</th>
<th>Location of fracture</th>
</tr>
</thead>
<tbody>
<tr>
<td>Optim 700 QL</td>
<td>762</td>
<td>859</td>
<td>12</td>
<td>Base plate</td>
</tr>
<tr>
<td>Optim 700MC Plus</td>
<td>725</td>
<td>843</td>
<td>14</td>
<td>Base plate</td>
</tr>
</tbody>
</table>

The final fractures in the base plate demonstrated that the joints were matching or overmatching.

Bending tests were carried out according to the standard SFS-EN 910 [14] using transverse face and root bend specimens, which were bent to an angle of 90 ° over a mandrel diameter of 61 mm for Optim 700 QL and 56 mm for Optim 700MC Plus. Only the root bend specimens of the joint of Optim 700MC Plus steel were acceptable. All other test specimens cracked from the welds.

Charpy V impact toughness testing was performed on the butt joints at -40 °C with the notches located in the middle of the weld, at the fusion line and in the HAZ at distances of 1 mm and 3 mm from the fusion line. The test results fulfilled the impact energy of 20 J using single-sided notched 7.5x10 mm specimens for 10 mm thick plates (Fig. 5). The impact toughness of the butt joint of Optim 700MC Plus steel was a little better than that of Optim 700 QL.

![Impact toughness of laser-GMA hybrid welded butt joints of Optim 700 QL and Optim 700MC Plus.](image)

The hardness profiles (HV 5) were measured across the joints on the face and root sides using measuring intervals of 0.25 mm from the fusion line to the base plate. The average hardness values of the weld and HAZ were higher than those of the base plate. The hardness values of the laser-GMA hybrid welded joint of Optim 700 QL steel were about 70-80 HV 5 higher than those of Optim 700MC Plus steel (Fig. 6).
Figure 6. Hardness profiles of laser-GMA hybrid butt welded joints of Optim 700 QL (a) and Optim 700MC Plus (b).

Macrostructures of the joints are given in Fig. 7. Microstructures of the weld and HAZ of the laser-GMA hybrid welded joints are given in Figs. 8-9.

Figure 7. Macrostructures of laser-GMA hybrid butt welded joints of Optim 700 QL (a) and Optim 700MC Plus (b).

Figure 8. Microstructures of laser-GMA hybrid butt welded joint of Optim 700 QL: Weld metal (a) and coarse grained HAZ (b)
5 Discussions

5.1 Comparison of steel grades Optim 700 QL and Optim 700MC Plus

A direct quenching and tempering manufacturing process of Optim 700 QL steel can provide significant energy saving and logistical advantages compared with the conventional quenched and tempered manufacturing process. However, this manufacturing process is challenging due to a strict control on the total manufacturing process including chemical composition of steel, steelmaking, thermo-mechanical rolling, quenching and tempering to achieve a homogenous microstructure and uniform properties in the plates. Due to a relatively high carbon and alloy contents of steel the successful laser and laser-GMA hybrid welding requires an optimal combination of welding parameters and consumables.

On the contrary, the thermo-mechanically processed strip steel Optim 700MC Plus together with the laser and the laser-GMA hybrid welding opens up more economical solutions to structures where the material thickness is below 12 mm if compared to the steel Optim 700 QL steel. The relatively low carbon content and carbon equivalent of the steel improves weldability and properties of the joints.

5.2 Laser-GMA hybrid welding characteristics

Welding tests showed that it was possible to weld a 10 mm thick plate of steel Optim 700 QL at a speed of 1.8 m/min and Optim 700MC Plus steel at a speed of 2.28 m/min using 12 kW disc laser-GMA hybrid welding. A welding speed of 1.8 m/min for Optim 700 QL steel corresponds with the welding speeds of 1.6-1.8 m/min used in the previous disc laser-GMA hybrid welding with 8 kW disc laser [8]. Lower hardening susceptibility of steel Optim 700MC Plus made possible to use a higher laser welding power of 11 kW and welding speed of 2.28 m/min than for Optim 700 QL steel.

In the laser-GMA hybrid welds, weld defects like porosity and a lack of penetration occurred affecting on cracking of the welds in the bending tests. The bending properties of the joint of Optim 700 QL steel were poorer than that of Optim 700MC Plus steel probably due to higher hardening of the weld especially in the root side of the weld.
5.3 Mechanical properties and microstructures

The laser-GMA hybrid welded joints were overmatched compared with the base plate because of the very low heat input and using matching welding wire Böhler X70-IG. High hardening in the root side of the weld and in the coarse grained HAZ increased the strength of the joint of Optim 700 QL steel. Also lower hardening in the joint of steel Optim 700MC Plus was sufficient to increase the strength of the joint higher than that of the base plate.

The impact toughness of the welded joints fulfilled the requirement of 20 J at for 7.5x10 mm specimens at -40 °C and was a little better than CO2 laser welded joints of the conventional quenched and tempered steels [15,16]. In these welded joints, the impact toughness of the weld was better than that of the fusion line and the coarse grained HAZ due to a fine lath martensitic microstructure. On the contrary, a coarse lath martensitic microstructure impaired toughness of the coarse grained HAZ of Optim 700 QL steel compared with a bainitic microstructure in the coarse grained HAZ of Optim 700MC Plus steel.

6 Conclusions

Manufacturing high strength steel grades with the yield strength of 700 MPa using direct quenching and tempering process and thermo-mechanical processing together with welding of modern disc laser-GMA hybrid process opens up possibilities for significant energy saving and logistical advantages in steelmaking and the fabrication of structures compared with conventional processes.

In this research work, joints with a 10 mm of steel thicknesses were welded using 12 kW disc laser-GMA hybrid welding process at laser powers of 8.7 kW and 11 kW and speed of 1.8 m/min and 2.28 m/min for Optim 700 QL steel and Optim 700MC Plus steel, respectively.

Overmatched joints were achieved due to hardening weld and the HAZ as a result of the very low heat input also in the joint of thermo-mechanically processed strip steel Optim 700MC Plus.

The impact toughness of the joints fulfilled the requirement of 20 J for 7.5x10 mm specimens at -40 °C and it was better in the HAZ of Optim 700MC Plus steel than that of Optim 700 QL steel. The coarse lath martensitic microstructure in the coarse grained HAZ of Optim 700 QL steel impaired the toughness compared with the bainitic microstructure in coarse grained HAZ of Optim 700MC Plus steel.

7 Acknowledgements

This investigation has been performed within the laser project at Rautaruukki Oyj. The project was funded by the Finnish Funding Agency for Technology and Innovation. The authors would like to thank the personnel of Winnova Laserpro for welding of the test pieces.
8 References

DIFFERENCES BETWEEN ARC MODES IN LASER HYBRID ARC WELDING UPON WELD BEAD STABILITY AND UNDERCUT FORMATION

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Abstract

In this study, three different arc modes are studied in laser hybrid arc welding with a gas metal arc, i.e. Standard, Pulsed and Cold Metal Transfer mode. Originally developed for being able to weld thin materials, the pulsed mode is the favoured arc mode in both ordinary arc welding and hybrid welding. The pulsed mode is a more controlled gas metal arc welding process that uses less heat and is able to weld thinner materials than the spray mode process with globular drop transfer. The cold metal transfer mode utilizes surface tension drop transfer, compared to the free flying drops governing the other modes and is thus even more controlled than the pulsed mode. The cold metal transfer mode is much colder than the other arc modes and is considered to generate less undercuts and spatter than the other modes, by both developers and users alike.

This study compares welds made by the three arc modes for both low and high deposition rates. The welds are studied by macrographs, scanning and high speed imaging. This study shows that the differences between drop transfer modes are partially eliminated due to the presence of a laser keyhole. The main arguments to use either arc mode are discussed.

Keywords: Laser, hybrid, arc, welding, undercut, bead, reinforcement, stability

1 Introduction

Laser arc hybrid welding [1-4], LAHW, combines a focused high power laser beam with an electric arc in the same process zone. An advantage compared to autonomous laser welding is the addition of filler wire to fill gaps and the surface shaping abilities of the filler metal done by the arc. In arc welding, different techniques can be used, such as the common Standard (natural) arc mode with various drop transfer modes (e.g. spray, short circuit or globular) depending on current and wire feed rates. Commonly, LAHW operates with the MIG/MAG in Pulsed arc mode, where one-drop-per-pulse is released and transferred in a semi-controlled flight towards the melt pool [5,6].

Recently, another yet more controlled Pulsed arc mode technique was developed that utilizes surface tension drop transfer. This technique is called Cold Metal Transfer, CMT [7],
Fig. 1. Drawing of laser hybrid arc welding setup, including geometrical units

where the wire is pulled back and forth instead of using a constant wire feed. Advantages are both that the drop transfer is smoothly delivered instead of flying into the pool and that less electrical power is needed by the arc to melt the wire. In ordinary arc welding, the CMT mode is preferably applied for thin sheets, where it also often enables welding of higher welding speeds, less heat input and better weld quality (e.g. less spatter, undercuts) compared to other arc modes. For thicker sections, the wire feeding mechanics often limits the deposition rate and the ability to fill grooves at higher welding speeds. Recently, the CMT was also suited for LHAW, welding single-pass 2 mm thick aluminium [8], 1 mm steel and multi-pass 15 mm steel [9,10].

The quality and strength of welds are significantly determined by surface geometry [11,12], that results from complex fluid flow mechanics caused by the electric arc, drop transfer and the laser [13]. Depending on arc mode, weld setup and parameter choice, the weld process may become unstable and resulting in varying surface geometry [14,15]. The basic physics of LAHW is still poor, but from X-ray imaging it has been observed that the melt pool is substantially elongated in the direction welding. High speed imaging (HSI) has enabled the study of drop transfer and keyhole conditions for steel [16,17] and aluminium [18]. Depending on presence of various gaps, different welding situations has been classified, affecting drop flight, heat and mass transfer [18].

Computational Fluid Dynamics (CFD) of the whole drop transfer and melt pool was achieved by some research groups, despite the required heavy computation [12,19]. However, analysis of the manifold phenomena involved is selective and limited.

In this paper, the CMT-technique is studied for LAHW for thick section material welding concerning weld stability and tendencies to avoid spatter and undercut formation. The CMT mode is compared to Pulsed and Standard (spray) arc modes for chosen wire feed rates within the limits by the CMT process. The effects on weld stability when enlarging the keyhole are also briefly studied.

2 Methodology

2.1 Welding equipment and setup

For the welding a laser, a MAG, plates and gas shielding were used. The laser used was a 15 kW Yb: fibre laser (IPG YLR-15000, fiber core diameter 200 µm, with a beam parameter product 10,3 mm mrad, and a wavelength of 1070 nm), operated at cw mode, focused at the surface by a 300 mm optics to a spot size of 400 µm diameter (Rayleigh length ±4 mm). To prevent back reflections, possibly damaging the optical fiber, some tilting was applied. An illustration of the setup of laser and MAG can be seen in Fig. 1, with setup parameters in Table 1. The MAG torch was applied in a tilted and leading position.

The MIG/MAG device used for all three modes (CMT, Pulsed and Standard) was a Fronius MAG power source

![Fig. 1. Drawing of laser hybrid arc welding setup, including geometrical units](image)
TPS4000 VMT Remote. The wire feeder is a combination of a continuous feeding unit VR7000 with a Robacta Drive unit (from Fronius) that carries out the back and forth motion of the wire tip which enables the CMT-process. Most of the parameters could not be chosen, but preset by the system at different wire feed rates with a chosen synergy curve. From these presets, some adjustments are allowed. The filler wire used was Lincoln SupraMIG Ultra, a steel-based wire with a diameter of $\varnothing=1.2$ mm. The plates welded were 7 mm thick Domex 420 MC with the mill scale removed and laser cut into pieces 50 mm wide and 300 mm long. The material composition of both the wire and the plates can be seen in Table 2. The applied shielding gas Mison18 (82% Ar, 18% CO₂) was used at a flow rate of 20 L/min.

Table 1. Weld setup parameters

<table>
<thead>
<tr>
<th>Parameter (unit)</th>
<th>$x$</th>
<th>$s$ (mm)</th>
<th>$d$ (mm)</th>
<th>$\alpha_L$ (°)</th>
<th>$\alpha_R$ (°)</th>
<th>$z_0$ (mm)</th>
<th>$f$ (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Value</td>
<td>+</td>
<td>18</td>
<td>2</td>
<td>7</td>
<td>-28</td>
<td>-2</td>
<td>300</td>
</tr>
</tbody>
</table>

Table 2. Material composition of work piece and filler wire

<table>
<thead>
<tr>
<th>Name</th>
<th>C (%)</th>
<th>Si (%)</th>
<th>Mn (%)</th>
<th>P (%)</th>
<th>S (%)</th>
<th>Al (%)</th>
<th>Nb (%)</th>
<th>V (%)</th>
<th>Ti (%)</th>
<th>Si (%)</th>
<th>Fe* (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Domex 420MC</td>
<td>0,10</td>
<td>0,03</td>
<td>1,50</td>
<td>0,025</td>
<td>0,01</td>
<td>0,15</td>
<td>0,09</td>
<td>0,20</td>
<td>0,15</td>
<td>98,01</td>
<td></td>
</tr>
<tr>
<td>Lincoln SupraMIG Ultra</td>
<td>0,08</td>
<td>1,70</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>0,85</td>
<td>97,37</td>
</tr>
</tbody>
</table>

* Displayed values are maximum, except for Fe, which is minimum.

2.2 Experimental procedure

In order to compare the different arc modes properly, a butt joint setup was chosen instead of common bead on plate tests, since studies have shown that the arc and metal flow is influenced by the presence of a gap [18].

The three arc modes are compared in this study are: CMT, Pulsed and Standard. The tests were performed with a close to zero gap with two different wire feed rates within the limits offered by the CMT. The CMT has a maximum feed rate of 8.3 m/min for a $\varnothing=1.2$ mm wire. The two wire feed rates, low and medium, are divided into Case 1 and Case 2. The three arc modes where run for both cases, resulting in six welds. The parameters used and the resulting average arc current, voltage and power, can be seen in Table 3.

To avoid too much bead reinforcement in Case 2 when wire feed rate was increased, welding speed was also increased. To test the effects of an increasing size of the keyhole, a final weld was made with the Pulsed arc mode, with the laser unfocused above the surface, using linearly increasing power.

2.3 Analysis

To evaluate the weld experiments, the top surfaces where scanned prior to and after welding. HSI was also used during the welding process [1,3,4] for each experiment to better evaluate the causes for changes in the top geometry between the welds. Experimental details for HSI and scanning can be found in [3,4].
3 Result and discussion

First, a comparison of CMT to Pulsed and Standard arc mode LAHW is presented, followed by a process behaviour explanation and topographical measurements (bead height and undercuts) for the six welds in the two studied weld cases. Finally follows a brief evaluation of the effects by increasing the keyhole size.

3.1 Mechanics of utilized arc modes

Sequences of HSI of the three different arc processes can be seen in Figs. 2b)-d). Only the process zone part is shown. The transferring drop, keyhole and molten weld pool are shown in Fig. 2a). As can be seen, the CMT mode creates a smaller gouge than the other modes, owing to the lower arc power needed to melt the wire. The varying transfer modes used here are surface tension for CMT, Fig. 5d), globular for Pulsed mode, Fig. 5c), and projected spray mode for the Standard mode arc, Fig. 5b).

The pulsed mode is usually applied for LAHW and it utilizes spray mode, but with controlled current to create globular drop transfer and a more stable arc. The CMT mode not only uses controlled current, but also controlled wire feeding to make the wire go back and forth and dip the drop at the wire tip (created by high current arc) at the weld surface (now at low current), enabling surface tension transfer [5,20].

Cross sections of typical hybrid welds (with a 0,5mm gap prior to welding) for the three modes can be seen in Fig. 3. In
The CMT mode causes a much more narrow HAZ and a slightly narrower upper fusion zone than the other two modes, due to the lower arc power needed for the same wire feeding. Figure 4 shows the average heat input into the workpiece made by the different arc modes in the two welding cases, specified in Table. 3.

![Figure 3](image.png)

**Fig. 3.** Laser arc hybrid welded cross sections, produced by combining a laser with a) spray arc, b) pulsed arc and c) Cold Metal Transfer arc mode. d) shows all three HAZ and fusion zones compared [20]

**Fig. 4.** Average power input into the workpiece produced by the arc for three arc modes and two welding cases

### 3.2 Process behaviour

The three different arc modes behave somewhat differently between the two welding cases. HSI of the three modes in the two welding cases can be seen in Fig. 5. When wire feed and welding speed increases from Case 1 to Case 2, the CMT mode creates larger drops with slightly higher frequency (surface tension drop transfer), but generally behaves the same. The Pulsed arc mode has the same (globular drop transfer governed by gravitational forces) size of drops by doubling pulse frequency and current. The Standard mode increases both voltage and current and changes from repelled globular drop transfer (governed by gravitational and repelling forces) to globular drop transfer (governed by gravitational forces).

For the CMT mode in both Case 1 and 2, Figs. 5a,d) respectively, the keyhole makes the melt area wider than the gouge region created by the arc and drop transfer does not disturb the keyhole. In Case 2, the arc pushes a wave of melt on top of the keyhole, but it does not seem to collapse. For both cases when using the Pulsed arc mode, Figs. 5b,e), the wire drops usually lands in the keyhole, but the process seems undisturbed. The melt flow is narrower and has higher speed compared to when using the CMT mode. Weld edges solidify before the main melt flow. The arc is less stable in Case 2 than in Case 1, occasionally producing spatter. When using the Standard mode in Case 1, Fig. 5c), the arc pressure enables a large drop to form, which causes a turbulent melt flow when it detaches and hits the melt zone beneath. In Case 2, Fig. 5f), globular drops are formed and the arc behaves slightly unregularly, creating an uneven gouge. Compared to Pulsed mode, the gouge is also deeper and the released drops are larger.

The drop transfer does not seem to really disturb the keyhole in either weld, leaving the power and size of the arc as the dominant factor for differences between the arc modes.

Macrographs of the final welds can be seen in Fig. 6. From a spatter point of view, there are only really spatter for weld Case 2 for the Pulsed and Standard arc modes, where the
Standard arc mode produces the most. Also, the welds made by the CMT arc mode show less variation.

<table>
<thead>
<tr>
<th>Process behavior of modes indifferent cases</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
</tr>
<tr>
<td>Case 1</td>
</tr>
<tr>
<td>0 ms, Drop formed</td>
</tr>
<tr>
<td>4.1 ms, Drop touches surface</td>
</tr>
<tr>
<td>8.0 ms, Arc ignited</td>
</tr>
<tr>
<td>10.2 ms, Drop released</td>
</tr>
<tr>
<td>185 ms, Drop released into melt</td>
</tr>
</tbody>
</table>

| Case 2                             |              |               |             |
| 0 ms, Drop formed                  | a)           | b)            | c)          |
| 3.5 ms, Drop touches surface       | d)           | e)            | f)          |
| 7.3 ms, Arc ignited                | g)           | h)            | i)          |
| 4.5 ms, Arc ignited                | j)           | k)            | l)          |
| 7.0 ms, Drop released into melt    | m)           | n)            | o)          |

**Fig. 5.** High speed image sequences (60° inclined from surface) for three arc modes in two laser arc hybrid welding cases. Arc mode CMT is shown in a,d), Pulsed is b,e) and Standard is c,f)

### 3.3 Topographical results

For the six welds shown in **Fig. 6, Fig. 7** shows the scanned hybrid weld surface topography, starting at \( x = 0 \) and ending at \( x = 300 \) mm. The scan lines are made 0.5 mm apart in the \( x \)-axis with approximately 300 data points for each line in the \( y \)-axis.

From the scan results, the bead height and undercut \( z \)-values can be derived for each scan line along the \( x \)-axis. The bead height is traced in the \( y \)-direction for each scan line. Undercuts, left and right, are deduced by looking at the lowest values beside the bead. As an example, the resulting graph for the CMT arc mode in case 1 is plotted in **Fig. 8**, where the vertical axis is the height and the horizontal axis is the length. From these values, maximum, minimum, average and standard deviation values can be deduced. The standard deviation \( (\sigma) \)
is a measure of how much deviation there usually are from the mean average value. It is calculated by

\[ \sigma = \sqrt{\frac{\sum (z-\bar{z})^2}{n-1}}, \]  

(1)

**Fig. 6.** Top surface of laser arc hybrid welded top surfaces for three arc modes and two weld cases. Only in weld Case 2, Pulsed and Standard arc mode suffers from spatter.

**Fig. 7.** Plotted scans of laser arc hybrid welded top surfaces for three arc modes and two welding cases. Utilized arc modes are CMT a,d), Pulsed b,e) and Standard c,f)
where \( \bar{z} \) is the mean average, \( \bar{z} \) are the height values and \( n \) is the sample size. The standard deviation can be used as a measure of stability for the process. If the value is low, the weld is regarded as having good stability. But on the contrary if the value is high, the weld is regarded less stable as it has more variation. In Fig. 8 and following analysis, the weld start and stop are always excluded. Average, min/max and std. dev. values are derived for the six topographies in Fig. 7 and plotted as graphs in Fig. 9 (with left and right undercut combined as average value).

![Case 1: CMT + laser - Weld bead geometry](image1)

**Fig. 8.** Graph of traced geometries for a laser CMT arc hybrid weld. Bead height and undercut curves are plotted along with markings for standard deviation and min/max values

![Weld bead height and Undercut depth](image2)

**Fig. 9.** Column charts for three arc modes in laser arc hybrid welding for two welding cases, showing statistics for a) bead height and b) undercut depth

When looking at the weld bead height stability (standard deviation), it can be seen for both cases that the CMT arc mode varies the least, while the Pulsed mode varies more and the Standard mode varies the most. The graph for Undercut depth shows the same trend concerning both stability and actual depth. It can be concluded that the weld bead height stability and undercut formation are interlinked. It has earlier been established that humping and undercuts usually occur at the same time in pure arc welding [21,22]. It can also be seen that the undercut depth do not vary between the cases when using the CMT arc mode, even though the bead height instability is doubled. This is probably due to that the arc only creates
a small gouge and the weld width is later increased by the laser, effectively eliminating undercuts created by the arc, as seen in Figs 5a),d).

3.4 Impact of the arc upon undercuts and stability

The graph in Fig. 10 is made by measuring the average arc width and height for each weld from the videos in Figs. 5 a-f) and using the average arc power from Table 3. These values are combined with the average and std. dev. values from Fig. 9. It can be seen that the size of the arc itself do not have a big impact on neither undercuts nor bead stability. The CMT (when arc pulse is high) and Standard mode has the same arc size. The arc power shows good connection for with bead height stability. Undercut formation is also connected to arc power, but the CMT mode is much less affected than the Pulsed or Standard arc modes.

3.5 Laser keyhole size effects upon weld bead

An additional experiment was made to test laser power and keyhole size impact on the weld bead stability and undercut formation. The parameters used was the same as in Case 2 Pulsed mode, but with the following modifications; \(d = 3\, \text{mm}, \, v = 3\, \text{m} \cdot \text{min}^{-1}, \, z_0 = 10\, \text{mm}\) and \(P_{\text{laser}} = 4 - 7.6\, \text{kW}\) linearly increased. Full penetration occurred when \(P_{\text{laser}} \approx 4.6\, \text{kW}\) and \(x \approx 50\, \text{mm}\). Figure 11 shows the resulting scan topography and with the bead height and undercut graph.

In the graph it can be seen that something happens with the undercut formation after \(x \approx 130\, \text{mm}\) when \(P_{\text{laser}} \approx 4.6\, \text{kW}\). The process is partially shown by HSI in Fig. 12, where Fig. 12a) shows a stable process while Fig. 12b-c) shows an unstable arc with a keyhole that changes in size. The disturbances of the arc might be because of irregularities in the amount of vapour above the keyhole (that alternates in size) produced by the laser. The metal vapour is electrically conducting [23] and could affect the shape of the arc. Another possible cause for the unstable arc could be disturbances in the shielding gas flow.
4 Acknowledgements

The authors are grateful for funding by VINNOVA – The Swedish Innovation Agency for the project RobuHyb, no. 2011-01782. Contributions from the Swedish companies are highly appreciated.

5 Conclusions

For the here studied cases, having low to medium wire feed rates and medium to high welding speeds respectively for a butt joint of 7 mm thick steel plates with a close to zero gap, these conclusions are made:

- Undercut minimizing and weld bead height stability is best with the CMT arc mode, followed by the Pulsed mode and then the Standard arc mode.
- For the same wire feeding, the CMT arc mode conducts less heat into the workpiece than the Pulsed or Standard arc mode hybrid welding; however, this might require more laser power to achieve the same penetration depth.
- Having a small arc gouge region gives preferable melt flow and solidification, resulting in less undercut and a more stable weld bead.

Fig. 11. a) Plotted surface of laser arc hybrid welded surface with linearly increasing laser power and b) shows the traced bead height and undercuts

Fig. 12. High speed image sequence of a laser arc hybrid weld with a linearly increasing laser power at an unfocused and elevated position. At a) the process is regularly stable, while b,c) shows a disturbed arc process
Drop transfer mode effects are mostly eliminated by the keyhole, except when the drop disturbs the keyhole too much.

When the current is high, the CMT and Standard modes have equally sized arcs; Pulsed arc mode has the largest arc.

- Arc size has weak correlation of undercut formation and bead stability.
- Increasing the average arc power seems to increase undercut formation and decrease bead stability for Pulsed and Standard mode arcs.

When welding at high speed with a medium wire feed rate, the arc becomes unstable for Pulsed and Standard arc modes and spatter is produced, most for Standard mode.

Having a keyhole that varies in size seems to disturb the arc, promoting undercut formation.

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AN EXTENDED FEASIBILITY STUDY TO IDENTIFY LASER WELDING AND LASER-ARC HYBRID WELDING BUSINESS CASES AND BUSINESS MODELS

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Abstract

The laser research group at Luleå University of Technology has, in collaboration with Swedish industries, performed more than 20 laser welding and laser arc hybrid welding case studies during the last decade. Most of them can be considered as successful and have demonstrated improved mechanical properties, new design opportunities and higher welding productivity. Nevertheless, very few of the successful case studies have resulted in an industrial application and the number of industrial implementations is still very limited. There are of course several reasons, like company organisation, human factors or lack of experience, but usually it is a rational analysis based on return of investment. The case studies are also usually limited to one component at one company without any further evaluation and analysis of other business opportunities, i.e. other components/products within the company or forming a cluster of companies in a geographic region. This paper presents an extend analyses of a number of the performed case studies with the objectives to evaluate and identify business cases. Different business models, such as investigate product families for laser welding within a company and company clusters with common laser welding resource, is evaluated and analysed.

Keywords: laser welding, laser arch hybrid welding, business case, cluster

1 Introduction

Laser-arc hybrid welding (LAHW) can, together with its high process and properties abilities, offer the use of innovative joint design and advanced material in the product and process development. Hybrid welding has been and is investigated by industry through several research projects in Sweden [1,2], the Nordic countries and the European Union. Nevertheless, the number of industrial application is still limited and one important reason for this is the lack of references, experiences, standards and therefore the necessary confidence among product designers to apply laser hybrid welding.
LAHW combines the best attributes of laser and arc fusion welding processes (GMAW) [3]. The process enables to produce welds which are still characterized by low distortion, high reproducibility and good fatigue performance as typical for laser welds. The addition of the electric arc fusion process permits greater fit-up tolerance, elimination of defects and the ability to weld thicker sections with a given laser power [3]. Higher productivity, profitability and quality of the welding process has been reported [3,4]. The laser arc hybrid process is characterized by the simultaneous application of a focused laser beam and an arc, creating and moving a common melt pool along the weld pass, see Fig. 1. The most important disadvantages with LAHW are, compared to MAG-welding, 6-10 times higher investment cost, higher safety requirements and, maybe most important, so far much less knowledge and confidence for LAHW within industry [1]. The combination of two welding processes in LAHW also implies a high number of parameters to control and manage. This, together with the fact that the GMAW controllers seldom are fully adapted for LAHW in terms of synergy lines etc., makes it challenging and often time consuming to find optimum process parameters.

Fig. 1: The laser-arc hybrid welding technique [2].

The shipyard industries have been one of the biggest adapters of LAHW. This has been driven by ship design requiring weight reduction which has increased the use of thin material, less than 10 mm thick, that implies welding structures that are more sensitive to heat inputs [4]. A comparison between the traditional applied submerged arc welding (SAW) or tandem submerged arc welding (TSAW), and LAHW shows several advantages for LAHW. For butt-joint welding of 5-mm panel seams and inserts, SAW and TSAW imparts approximately 5.6 and 3.4 times as much total heat than LAHW [4,5]. The distortion of the plate is also drastically reduced, leading to less post welding correction work. A reduction of the total production cost of more than 60% was reported [5]. This example clearly demonstrates important keys for successful implementation in an industrial branch, a direct positive impact on the shop floor level in terms of lower costs together with new design opportunities that gives clear advantages for the final customers in terms of a higher payload ship weight ratio. In the product design phase, where the joint method is selected, welding standards play an important role but these are only applicable for traditional welding processes and more or less useless for the new welding geometries which can be achieved by laser hybrid welding.
is an on-going project at the International Organisation for Standardization to develop a standard for hybrid laser welding but this work is still in the early phases. Thus, today an implementation of hybrid laser welding in industry requires product specific process development and mechanical testing. This time consuming development step together with the lack of confidence has a negative effect on the internal dialogue between designer and process developers. A dialogue is required for a more effective and intensive implementation of laser hybrid welding.

Jackson [6] has presented and demonstrated a new concept, Factory-in-a-Box, that is describe as a “mobile production capacity on demand”. The key features of the concept are flexibility, mobility, and speed. The concept consists of standardized modules that can be installed in containers and easily transported by, e.g. trucks, rail vehicles, boats etc. The modules shall be easy to combine into complete production systems and easy to reconfigure for new products and/or scaled to handle new volumes. A similar concept has been developed by ABB Robotics they can today offer a complete turnkey, plug and produce, robot welding station, FlexArc, for GMAW. The cell is based on a steel frame structure that can be installed and up running within less than 4 hours, see fig. 2.

![FlexArc, ABBs robotic arc welding cell](image)

These developments offer an opportunity for industries to share high cost investment and thus reduce the risk in the implementation of new manufacturing technologies. The advantages and disadvantages with this approach for an industry cluster will be further discussed later in this paper.

2    Industrial case study example

Cargotec Sweden AB has together with Luleå University of Technology taken part in the LOST project with the aim to analyse if it is possible to use the laser welding technique to build loader cranes with less weight and/or higher capacity. The case [8] described here has had the aim to reduce weight by moving a longitudinal weld in hydraulic hexagonal extensions from the most stressed point to the neutral plane. In this case, the hexagonal cross-
section has been divided into two halves to make it possible to position the weld into the neutral plane of the cross-section, see Fig. 2, where the stresses from both global beam bending and the local shell bending from the slide pads are close to zero. By that, the weld will not be exposed to fatigue load, and it will be more advantageous to use high strength steel with high resistance to static stresses.

![Fig. 2: Present design and redesign of the telescopic extensions [9].](image)

The extension beams were successfully welded with full penetration, without any welding defects and with a weld geometry generating low stress concentrations, though it not was an issue in this case. Other results from the first case study are summarised and compared with the present method in Table 1.

### Table 1: Comparison of main performance measures between LAHW and MIG for the telescopic arm case

<table>
<thead>
<tr>
<th>Quantity</th>
<th>LAHW</th>
<th>MIG</th>
</tr>
</thead>
<tbody>
<tr>
<td>Productivity (time to weld an extension)</td>
<td>1.3 minutes</td>
<td>4 minutes</td>
</tr>
<tr>
<td>Line energy</td>
<td>Appr. 190 J/mm</td>
<td>Appr. 1400 J/mm</td>
</tr>
<tr>
<td>Welded metal deposition for one extension</td>
<td>Appr. 0.12 kg</td>
<td>Appr. 0.9 kg</td>
</tr>
</tbody>
</table>

The much lower line energy is an important enabler for the new two section weld design. As can be seen in Table 2 there are, as for the shipyard example discussed earlier, several advantages with the LAHW process and new design compared to the MIG process and current design, both for the manufacturer in terms of lower cost (shorter process time, less post welding work, less filler material and sheet material) and for the final customer in terms of reduced weight of the loader crane extension system. The total weight reduction will be about 220 kg for a complete crane which means higher payload and lower fuel consumption for the customer. The later will also have a positive environmental effect in terms of reduced carbon footprint, etc.

The case described above appears to be good candidates for a successful implementation of LAHW. However, one of the big obstacles for LAHW implementation is the high investment.
cost, which is about € 600 000 for this case. That needs to be compared with a total investment cost of about € 100 000 for an industrial robot MIG station. A real business case for a LAHW implementation therefore requires much high utilization compared to the MIG implementation. The total utilization is for loader crane component, about 500 hours per year. This is way below what is needed for a feasible business case with acceptable payback. Another issue are the safety requirements which are especially critical for low way length lasers (i.e. diode, fibre, disc and Nd:YAG-lasers) which requires a completely sealed-off work station together with safety training of the staff.

3 Categorisation of industries

This chapter introduces the different industries in the study including a categorisation to indicate there internal opportunity for successful laser welding (LBW) or laser arc hybrid welding (LAHW) implementation. The categorisation is based on:

- Organisation size, skill and welding awareness, C1
- Level of automation, C2
- Internal LBW and LAHW demand, C3
- Market opportunities/risks, C4

The results from the study are summarized in table 1.

**Table 2: Industry data and category**

<table>
<thead>
<tr>
<th>Industry</th>
<th>Region</th>
<th>Size</th>
<th>Laser Exp.</th>
<th>Process</th>
<th>Joint</th>
<th>Complexity</th>
<th>Volume</th>
<th>C1</th>
<th>C2</th>
<th>C3</th>
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<td>&lt;300</td>
<td>M</td>
<td>LBC/LBW</td>
<td>BUTT/FILL</td>
<td>MED</td>
<td>M</td>
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<td>2</td>
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<td>M</td>
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</table>

Each sub category has been given a number between 0 and 3 which implies:

- C1 (Organisation/awareness): 0: Small organisation, low skill and awareness – 3: >100 employees, internal welding expertise and some laser weld experiences
• C2 (Automation: 0: Manual – 3: High number of industrial robot welding cells

• C3 (LBW/LAHW demand: 0: None known internal need – 3: High volume close to implementation

• C4 (Market: 0: Declining demands/high risk/short forecast – 3: Stable demands/low risk/long forecast

The purpose with the categorisation is to identify industries with an internal potential for LBW or LAHW implementation and it is clear that industry 5 have the highest potential followed by industry 1. The status for industry 5 is that a full scale industrial demonstrator is under development, in collaboration with Luleå University of Technology, and is expected to be delivered and evaluated in the beginning of the autumn 2013. The total number of laser welding hours per year is estimated to about 2000 hours and the total investment (laser sources, welding equipment, manipulator etc.) is estimated to €900,000 for the LAWH cell and €400,000 for the GMAW. The differences are mainly due to the cost for the laser source, in this case a 6 kW fibre laser. Other important factors to be able to evaluate the business case are:

• The welding time which is estimated to be about 40% for the LAHW compared to GMAW.
• The operation cost per hour, exclude the capital cost and operators, which is estimated to be about 50% higher for the LAHW compared to GMAW.
• The number of production hours per year is about 2000 hour for LAHW and 5000 hour GMAW.
• The capital cost per hour, based a 5 year payback time, is LAHW about 200% higher for the LAHW than for the GMAW due to the higher investment and lower utilization for the LAHW alternative.
• The operate cost per year, based on one operator, is 50% for the LAHW compare to the GMAW.

The operation cost per hour is estimated €20 for LAHW and €12 for GMAW, excluding filler material and shield gas; and the operator cost €70 per hour. The costs per product and year is summarised in figure 4.

![Fig. 4: Cost per product for LAHW and GMAW for industry 5.](image)
compared to the GMAW alternative, even though the utilization is just about 2000 hour per year. The estimations has also been quite conservative without including any additional advantages (i.e. less post welding correction work, increased quality) that can be expected for the LAWH alternative. However further investigation to proof the process robustness; overall effect on the manufacturing system together with more detailed investment analyses is needed to final decided.

The same calculation for industry 1 gives a somewhat higher product cost for the LAHW alternative compared to the GMAW alternative due to a lower utilization (about 1200 hour per year) and an opportunity to utilize existing equipment for the GMAW alternative.

4 Cluster formation

The next step is to investigate the opportunity for industry cluster formation and evaluate different business models within that cluster. An cluster index (CI) will be used which is defined as:

- The distance between the industries within the cluster, in this case clusters with industries within a radius if 100 km is considered (defined as regions in table 2).
- The total sum of the category, C1-C4, defined in table 2 for the industries within a specific cluster. Observe that an industries category sum must be above 4 to be considered in the cluster formation.
- Other market opportunity (i.e. bigger industries) with a laser process demand). The cluster index (CI) will be multiply by 2 if that opportunity in considerably.

Based on this we can identify two regions with high cluster indexes:

- Region A with industries 1, 3 and 15. The total sum for the categories is 21 (9+7+5) there are considerable other market opportunities in the area which gives CI=42.
- Region D with industries 7, 8, 13 and 14. The total sum for the categories is 24 but the market situation is much more unpredictable than for region A so the market opportunities is not considered which gives CI=24.

The rest of the analyses will be based region A which is considered to have the greatest opportunities. The three industries within the cluster have also a supply chain relation, and thus trust, which further strengthen the cluster. The total need for LAHW or LBW is approximately 2500 hour per year within the cluster.

One possible, traditional, business model for region A is that the LAHW station is installed at one of the industries which will act as a sub supplier for the two other industries. The investment and external revenues might be shared in proportion that reflexes the utilization for respective industry. Industrial 1 is the best location for the installation since they have the highest utilization and structures that are most complicated to transport. There are two mayor drawbacks with this approach:

1. The transports, lead time and work in progress (WIP) will increase for industry 3 and 15. The transport might be an issue for industry 3 since there welded structures are complex to transport as well.
2. Some of the other market opportunities will get lost due to the need of onsite laser processing.
A second possible business model is that a separate company invest and provide a movable LAHW and LBW station, a Laser Welding Cell in a Container (LWCC), including the expertise need to perform the welding task. An agreement regulates how the LWCC shall be distributed within the cluster to minimise the negative drawback in term of higher WIP and longer lead times. All three industries demands (product size, welding geometries etc.) as well as market opportunities has to be considered in the design of the LWCC. Other important user demands and thus design factors are effective transportability and short installation time. One drawback is that this alternative requires a heavy truck for transport between and handling within the industries which drastically reduce the flexibility (i.e. how often the LWCC can be transferred to be cost effective).

A third, version of the second, possible business model is that a separate company just provides the laser sources (fibre or diode) and the laser welding processing head which is transported between and installed in a new or existing welding station at the different industries in the cluster. The main advantages compare to the second business model is a that the laser sources etc. will be much easier to transport and thus can offer a higher flexibility with an accessibility for the industries in the cluster on a daily bases. The main drawback is that each industry needs to invest in a new welding station or adapt an existing station in terms of interface and laser safety requirements. However, an investment in a new welding station is estimated to be about 25% of the total investment and the welding station can be utilised for conventional welding as well. This business model also gives the opportunity, probably greater than for business model 2, to offer onsite laser processing to other industries in the region.

To evaluate the different business models requires and more depth analyses of the three industries including the effect of the manufacturing system (i.e. buffer size, transport etc.) as well as a market analyse. However, the third alternative appears to be attractive since it offers good combination of flexibility, minimising the negative effects of the manufacturing system, and risk sharing together with the best opportunity to penetrate the market opportunities. A rough estimation of the cost and revenues for the different business models can be seen in figure 5.

Fig. 5: Yearly cost and revenue estimation for the 3 business cases.
5 Discussions

This paper outlines a more comprehensive approach to go from a small, relatively successful case to an actual increase in the number of LBW and LAHW implementations based on an industrial cluster approach. The first step is to analyse the current welding situation at the company to answer questions such as the impact of welding, the total amount of welding, current welding methods and how “optimum” they are. This method is especially applicable for companies with low category index in table [i.e. I16-I25]. That shall also include opportunities for other welding methods and new minor product design. However that will in most case not results in a direct business case within a specific industry. A further, extended, study is need to identify industrial clusters with a critical mass (i.e. product volume, knowledge, market stability) to motivate the investment in LBW or/and LAHW. The methods and criteria for the forming this clusters has to be further developed but two important factors is the geographical distance and level of trust (i.e. current supply chain).

The cluster investigation and formation shall also include different business models, for instance an outsourcing of components that is suitable for LAHW to a local supplier to “share” the investment costs with other manufacturers in the area or different level of mobile LAHW approaches.

Finally the development of diode laser sources has to be mentioned which is expected to deliver high power (>8 kW) compact laser sources with acceptable beam quality for LBW and LAHW, and for a drastically lower price compared to fibre and disc lasers. This will drastically reduce the investment cost and makes the LBW and LAHW processes also profitable at lower volumes.

6 Conclusions

i. Laser-arc hybrid welding and laser welding offers a great potential to challenge current welding processes and product design.

ii. The low line energy offered is an important enabler for new welding procedures and product design.

iii. When comparing LAHW and MIG-welding, a ratio of 1/3, 1/7 and 1/8 was obtained for the welding time, line energy and filler material, respectively.

iv. The high investment costs are still, along with the limited knowledge and lack of confidence in industry, the main limited factors for LAHW and LBW implementations.

v. A more comprehensive approach, including the whole welding situation within the company, or a network of companies, to enhance the number of LAHW and LBW implementations has been demonstrated.

vi. Different business models are needed to be evaluated to identify the most effective way to share and distribute the cost and risk.
7 Acknowledgements

The authors acknowledge funding by EU Structural Funds / Objective 2 (project IndLas, no. 152512), EU-FP7 (project HYBRO, no. RFS-CR-12024) and VINNOVA (project LOST, no. 2006-00563 and project HYBRIGHT, no. 2005-02895).

8 References

LASER-WELDED SANDWICH FLOOR PANEL FOR MARINE CONTAINER

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²Ruukki Metals Oy, Uusikaupunki, Finland

Abstract

• A new type of floor structure for marine containers utilizes both the excellent properties of wear-resistant steel produced by Finnish steel maker Rautaruukki and the advantages of laser welding. The steel has excellent properties that protect it against wearing and impact. Laser welding has a low heat input, causes only minor distortions and has a high welding speed. The sandwich structure of floor has three parts: a top plate (t=2.9 mm), cores (t=2 mm) and a bottom plate (t=2 mm). The material of the top and bottom plates is the wear-resistant steel Raex 400. The shape of the cores is Vf and the material is weather-resistant steel Cor-Ten A. The joint type of the laser welds is a lap joint. The side profiles, which are made of Cor-Ten A (t=4 mm), connect the floor with the walls and are joined manually using GMAW. The weight of the marine container with the new floor is only 10% greater than the weight of a traditional container with steel beams and a plywood floor structure. The steel floor structure is patented by Rautaruukki. The actual performance testing of the marine container has been carried out according the standard ISO 1496/1.

• Keywords: Marine container, Raex, Cor-Ten

Nolamp 14 2013
Content

• Introduction
• Experimental part
  – Welding
  – Testing of container
• Conclusions

Introduction

• Manufacturing of the marine dry-containers happens mainly in China
  – Containers are done close to the end user to avoid transportation of empty containers

• The floor structure of marine dry-container has remained almost similar from the late 1950’
  – Steel beams (Weather resistant steel: Cor-Ten)
  – Sheets of plywood (Tropical hardwood)
  – Assembly of plywood by screwing

• Weather resistant steel is used for walls and roof of the container
  – Profiles of walls and roof are normally corrugated to get extra stiffness to the structure

• During life-time container meets very different conditions
  – Important: Surface treatment and protection against corrosion
Introduction

- Target of the research was to find a new solution for the floor structure of container
  - Alternative for plywood
  - Timber used for plywood is tropical hardwood (oily, last very well under marine conditions)
  - Timber is imported, mostly from Indonesia, Malaysia and Papua New Guinea
  - Amount of these wood species has been dramatically decreased
  - International Union for Conservation of Nature has tried set limits for the using of the tropical hardwoods

→ Availability of woods has decreased, poorer quality of plywood is on the market → limited lifetime of container

- Laser welded sandwich panel has many advantages:
  - Structure is very light, compared to alternative steel structures, stiff and can handle big loads
  - The structure has been used in shipbuilding and construction industry and also in some transportation applications

- Laser-welded closed sandwich panel and of high strength steels have been merged to get an optimal combination to the structure
Experimental part

- Traditional used 20ft marine dry-container was modified by replacing the old floor with the new type of laser-welded sandwich steel floor.
- Design and dimensions of steel floor were calculated by FEM
- Some details were cut away from structure e.g. forklift pockets

<table>
<thead>
<tr>
<th>Material</th>
<th>t mm</th>
<th>$R_{p0.2}$ N/mm²</th>
<th>$R_m$ N/mm²</th>
<th>$A_p$ %</th>
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</thead>
<tbody>
<tr>
<td>Cor-Ten A</td>
<td>2</td>
<td>404</td>
<td>533</td>
<td>30</td>
</tr>
<tr>
<td>Cor-Ten A</td>
<td>4</td>
<td>417</td>
<td>552</td>
<td>33</td>
</tr>
<tr>
<td>Raex 400 (typical)</td>
<td>2, 3</td>
<td>1000</td>
<td>1250</td>
<td>10</td>
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</table>
Experimental part

Welding

- Ruukki Metals steel service centre in Uusikaupunki is focused to laser cutting and welding
  - 3 laser systems: MC 1, MC 2 and MC 3 (Schuler Held)
  - Laser systems MC 1 and MC 3 were used
    - Equipped with CO2-lasers (Rofin Sinar), MC1=5 kW, MC2=8 kW
    - MC 1: Top and bottom plates (two sheets welded together to get a bigger plate, butt joint)
    - MC 2: Cores welded to top and bottom plates, lap joint
  - Welding parameters
    - Butt joint (MC1):
      - Top plate (3 mm): Laser power: 3.3 kW, welding speed: 2300 mm/min
      - Bottom plate (2 mm): Laser power: 2.8 kW, welding speed 2200 mm/min
    - Lap joints (MC2):
      - 2+2 mm: Laser power: 5.2 kW, welding speed: 2100 mm/min
      - 2+3 mm: Laser power: 6.4 kW, welding speed: 2100 mm/min
  - Shielding gas: Helium, flow rate of 20 l/min
Experimental part
Welding

- Welding of side profiles and final joining to frame of container was done by manual MAG welding

Testing of container

- Test standard EN 1496/1 (and ISO 15070) for dry container
  1. Stacking test
  2. Lifting from the four top corner fittings
  3. Lifting from the four bottom corner fittings
  4. Longitudinal external restraint
  5. Strength of end walls
  6. Strength of side walls
  7. Roof strength
  8. **Floor strength**
  9. Rigidity (transverse)
  10. Rigidity (longitudinal)
  11. Lifting from fork-lift pockets
Testing of container

- Just the test no. 8 was performed for the container
- Strength of floor is tested by using a test vehicle (demonstrates as a fork-lift) equipped with tyres, with an axle load of 5 460 kg (2 730 kg on each of two wheels)
- There is a certain size for tyres and the test vehicle is manoeuvred over the entire floor area of the container
- Permanent deformations that will render the use of container unsuitable are not accepted.
- Result of test: permanent deformation = local buckling
- Closed sandwich structure as a whole was very stiff

<table>
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<th>Testing point</th>
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<tr>
<td><strong>Condition</strong></td>
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<tr>
<td>Empty</td>
<td>35</td>
</tr>
<tr>
<td>After test</td>
<td>33</td>
</tr>
<tr>
<td>Difference</td>
<td>-2</td>
</tr>
</tbody>
</table>
Conclusions

- Test results of the new container floor were very promising
- The weight of laser-welded sandwich floor was just 10% higher than the weight of traditional floor structure
- Business potential on the manufacturing of dry-cargo containers is huge
- However, some development work is needed:
  - Local buckling of cores
  - Surface treatment against corrosion
R. Olsson

Laser Nova AB, Östersund, Sweden

Abstract:
Using Nd:YAG lasers in micromachining.
Important factors in mixed metal welding of dental products.
Remote cutting as an alternative to traditional CNC-systems in cutting of miniature medical devices.
Important factors.
Examples of solutions.

Keywords: Micro machining, Medical devices, Dental, Laser welding, Remote cutting, Dissimilar metals.

• 8 lasers. Fiber. Lamp pumped 10W-500W
  E.g
  − JK Lumenics
  − ROFIN
  − LASAG KLS 246 FC
  − ROFIN FL20 Q-switched fiber laser
  − LASAG CL8

• Microscopes, SEM etc.

Micro machining?
• Sizes in the μm - mm range
• Tolerances etc. measured in μm
Remote cutting as an alternative to conventional CNC

- ROFIN FL20
- Raylase AS-30 3D with camera output
- COGNEX Vision
- Basler CCD camera

- Go/ No Go based upon vision
- Oxygen as process gas
- Avoid excessive heat.

- 3D galvo → large mirrors → "slow at corners." → no "kW" Fiber, too much heat → Q-pulsed

- Fixturing!!!
Titanium grade II 128µ foil, welded against a 550µ ±5µ rod of NiTiNol.

- Shielding gas, argon
- Avoid oxides. (→ Brittle intermetallic phases)
- Pre etching.
- Pulse shaping
- Penetration ~100µ into the rod
- Basic idea: Certain surface structures stimulate bone growth. (Osseointegration)
- E.g implants in Titanium
- A process sensitive to pulse lengths, pulse energies, beam shape, speed, focal lengths, focal depth etc.
- Q-switched good old lamp- or diode pumped YAGs ~50W CW work
- Fiber lasers didn’t work. (not enough pulse energy)

Desired macrosurface
Tube welding

50 µm thick tube. Outer diameter 2 mm. Approx. 30µm penetration in the tube wall. LASAG CL8 3W CW. Pulse shaping!

Pulse shaping

1. Initial pre heating. Absorption
2. Melting phase
3. Stir
4. Gradually cool down. Reduce risk of thermal crack
Diameter 400 \mu m

Penetrate 50 \mu m into small tube

Rear and front welds

Rear weld

Syringes, welding

Diameter 400 \mu m

Penetrate 50 \mu m into small tube
Thank you for your attention!
LASER WELDING AT BROGREN INDUSTRIES – SUBCONTRACTOR FOR AEROSPACE, GAS TURBINES AND PARTS

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Abstract

Permanova Lasersystem AB has delivered a flexible laser robot station for laser welding, to the Brogren Industries AB in Älvängen, Sweden. The investment is part of the ambitious plans for three business units within Brogren Industries: Aerospace, Gas Turbines and Parts. Laser welding helps Brogren Industries strengthen its position as a supplier to aerospace and gas turbine industries even further, to get also small series contracts with more advanced materials.

The laser station is based on an 8 kW disk laser, with a robot and a 2 axis positioner. The robot is equipped with the Permanova WT03 welding tool, with Seam Tracking and Motorized Twin Spot Unit. This allows for high flexibility in joint geometries, as well as material properties, giving best properties in the final welded component. Examples of such advanced applications will be presented.

Key words: laser welding, aerospace, gas turbines, robot station, seam tracking, motorized twin spot, permaflex, disk laser.

1. Background

About ten years ago at Högskolan Väst a laser welding station was started for research. The station was delivered by Permanova based on the fiber and system technology they had developed. Research work continued and in 2006 Volvo Aero (today GKN Aerospace) started development of Laser Metal Deposition with Robots (RMS project). PTC (Produktions Teknisk Centrum) in Trollhättan was established and again Permanova supplied another laser station. The development of LMD process was refined and Volvo Aero also could see the potential benefit of laser welding without filler metal of subassemblies and simple parts to make complicated structures. The materials involved are all exclusive alloy types with high demands on the welding process. In 2011 Volvo eventually acquired a laser station for production of LMD and laser welding.

In the small town Älvängen some kilometers south of Trollhättan the Company Brogrens Industries was working to develop its machining business. They had seen their customer, ordering parts that the customer then would TIG weld together. They took the opportunity and invested in TIG welding capacity, they educated personnel, trained welders and used a robot for automatic TIG welding. This turned out to be a strategic decision and some years later the TIG welded products needed to be further developed and this could only be done with a key hole welding process.

2. Brogren Industries

Brogren Industries have a very modern machine park and highly competent personnel. The guiding principle for Brogren Industries has always been high quality in machined parts to customers in the industry. They have over the years worked towards a supply of components with machined and TIG welded parts and today they are a successful supplier to the gas turbine and aerospace industries.
3. The Challenge

When Volvo Aero placed an order for a large scale laser welding station Brogren Industries felt that the time was right to invest in a laser welding station. The discussion between Permanova and Brogren Industries had been going on for some time, when the requirements were clear Brogren Industries needed,

- Process flexibility
- Precision
- Performance
- Automation
- Innovation
- High welding speed
- Tolerances of final parts
- Ability to weld exotic materials
- Low heat input
- Minimal distortion
- Flexible positioning
- More efficient welding process

The station needed to be flexible in many ways. Weld geometries can have many sizes and geometries, large parts with long welds, circular parts with different diameters etc. Many different fixation solutions need to be possible.

Brogren Industries had presented parts from Siemens for tests that require deep narrow welds with high tolerance requirements on penetration and positioning. From discussions with Volvo Aero it was clear that complicated geometries could be necessary to adapt and find solid fixturing solutions for. The materials in the parts were different variant of high temperature resistant stainless steel qualities. The welded parts need to have minimal distortions and they must be within the final customer very high tolerance demands.

One pass laser weld. Several passes TIG weld

It was also a requirement that the welding process could be automated to permit higher production volumes than they had in their current TIG welding station.

Since Volvo Aero was an aspiring customer for welded parts Brogren Industries were of course interested in a station with similar capabilities and equipment as Volvo Aero’s

4. Technical Approach

Permanova and Brogren Industries had since 2008 had discussions and made tests on different parts to prove the possibilities for laser welding. Permanova presented a robot laser welding station with 8 kW disc laser, WT03 welding tool, equipped with Permanova seam tracking system, motorized twin spot, motorized focusing unit. The station design was large to suit for a working table, two-axis positioner and a large free space for future projects that may require different positioning and fixturing demands.
Permanova also could offer training and process development in the application lab at Permanova during the project and installation phase of the project. This meant that Brogren Industries could prepare themselves for the new technological step they were about to take before the installation was completed at Brogrens Industries. The principal equipment are: Laser Source TruDisk 8002, Process fiber 0.2 mm and 0.6, Welding tool, Permanova WT03 with seam tracking system, rotating collimation, Motorized Twin Spot Unit (MTSU), Motorized Focusing Unit (MFU), Cover Slide Monitoring (CSM), ABB IRB 4400 robot. ABB IRBP A-500, 500 kg, 2 axis positioner, Gaspanel for gas protection and cross-jet, Camera and Monitor, Primes ECPM48 for NIR-laser, Power meter.

5. Advantage

Laser welding is the method that has the most flexible and best performance vs. price characteristics of the alternative methods, e.g., plasma electron beam and TIG welding.

The core of the station is the ABB IRB 4400 robot. It is a good choice for laser welding applications, stable, high precision and strong to carry the welding tool. The welding tool WT03 that is equipped with a seam tracking system, motorized twin spot unit and motorized focusing unit is the core for the welding processes. It features the possibility to use seam tracking for on line tracking, off line tracking and programing robot paths.

To find an edge seam, a sensor is needed. If the seam is measured far ahead from the weld spot, it will be difficult to make sure that the weld spot follows the right path. It will be impossible to reach higher accuracies than that achieved by the robot - which is not good enough in demanding applications. A laser line is projected on the joint between the parts to be welded at an angle of 30-45° to the normal. This laser diode line, when observed from normal direction, is split up in x-direction (welding direction) because of geometrical difference and relief between parts. The camera takes an image. This picture is processed by an image processing PC, which generates a control signal and sends it to the servo unit, which then tilts the optical system around its rotation axis, thereby directing the laser beam accurately at the seam. The seam tracking system will compensate for inaccuracies in the robot movements, the part’s fixture and the parts. The robot will follow the nominal trajectory, i.e., the trajectory that is correct if the fixture, part and the robot all were perfect. The seam tracking system will then correct the deviation from this nominal trajectory. By looking close at the weld spot it is possible to use a simple approach to control the beam position. There is usually no need to account for any delay from the measuring spot to the beam position, so no need to rely on the accuracy of the robot, itself. On the tool there is a mechanism for moving the beam into the desired position. This mechanism tilts the optics to move the beam. Tilting the optics will make the movement very rigid and precise.

The Permanova MTSU is developed for applications where a second focus spot gives advantages, such as making the weld pool longer or wider in welding applications that require lower heat gradients or wider welds. The master spot is fix and the second spot can rotate around the master spot in the focus plane. The energy distribution can be varied continuously from 50/50% to 0/100% for the master spot/second spot, spot separation can also be varied; it is a very useful tool for quickly finding the best optimal process window. All parameters are quickly and easily changed via the robot program.

The Permanova Motorized Focusing Unit is developed to provide a good solution for applications requiring a flexible focus position, for instance due to product geometries. The Motorized Focusing Unit moves the focus plane with an electrical drive which allows programmed adjustments of the focus between two welds on the same component. The MFU can be adjusted via the robot control and is adjustable within -6 to +20 mm around the design focal length. The MFU also has a self-control that continuously measures the temperature in the unit.

One of the most critical process parameters of laser material processing is the beam power on the work piece. Due to absorption in the focusing optics, the beam power is reduced. The optics will be heated and the machining result will drift and finally the process may be lost. To avoid a reduction of the machining quality, the observation of power dissipation should be done at a regular interval over time as well as after maintenance activities. This can be done by a regular repetition of the power measurement. The EC-PowerMonitor not only measures the beam power in the process.
zone, but also possesses an integrated self-test function. Thus, the functional capability and the accuracy of the measuring device can be checked any time.

The ABB A positioner is suitable for applications where the parts have to be rotated around two axes to reach the optimal welding process. With the combination of the ABB IRB 4400 and the IRBP A500, easy programming and flexibility, many interesting part geometries and fixturing solutions can be accommodated.

6. Summary

The station is based on Permanovas PERMAFLEX concept for laser welding. With the above mentioned options Brogren Industries is capable to,

- Weld products with advanced geometries,
- Weld advanced alloys and joint configurations,
- Assure correct and stable welding processes,
- Have good profitability even for small series.

The station has opened the door for Brogren Industries to supply the Gas turbine and Aerospace industry with advanced and competitive solutions for parts that previously demanded more expensive and complex manufacturing.
FIBRE DELIVERED LASER BEAMS – AN ALTERNATIVE COST EFFECTIVE DECOMMISSIONING TECHNOLOGY

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Abstract

Decommissioning of a nuclear power plant is the systematic deconstruction of a contaminated, complex facility, which can be made up of large metallic components including the reactor vessel, steam generators, pumps, tanks and supporting systems, which often include many meters of tube networks. Remotely operated techniques and processes to cut waste material into smaller pieces are required that develop new and novel approaches that facilitate a smarter decommissioning process, ie one that is safer, faster and cheaper. Amongst the available thermal cutting processes for metallic components, multi-kilowatt, fibre delivered laser beams are well suited to remote deployment, due to the lack of reaction force, light and compact process heads and limited fume generation. Such lasers potentially offer increased cutting speed, high levels of automation, ease of deployment, flexibility of use and as a result, can be used to reduce volumes of radioactive waste through more selective cutting, thus reducing both costs and radiological risks. In addition, such lasers can be placed in an uncontaminated area making them reusable for many cutting tasks, as well as for decontamination of metallic and concrete surface, which most other cutting techniques are not capable to perform. This potentially makes the laser an alternative cost effective decommissioning technology. In this paper, the capability of a 5kW multi-mode laser is presented for cutting of unstructured tube networks in hazardous and confined nuclear environments. In addition, cutting results on thick plate material, representative of that which might be found in pressure vessels and dissolvers are presented. In addition highlights of industrially relevant demonstrations are also mentioned.

Keywords: Decommissioning, Laser, Remote-Cutting, Dismantling
1 Introduction

As of 2012, 138 civil nuclear power plants had been shut down in 19 countries, including 28 in the United States, 27 in the United Kingdom, 27 in Germany, 12 in France, 9 in Japan and 5 in the Russian Federation [1]. Only 17 of these power plants have been decommissioned so far. Decommissioning of a nuclear facility is a complex process that takes years. The cost of decommissioning nuclear power plants vary greatly, depending on the plant type and size, its location, the proximity and availability of waste disposal facilities, the intended future use of the site, and the condition of the plant and the site at the time of decommissioning. Each decommissioning task can be very different to the next, so an innovative and flexible approach to process deployment may be necessary.

In the United States, the estimated average cost of decommissioning a nuclear power plant is around US$500 million or approximately 10-15% of the initial capital cost. In France, the estimated cost of decommissioning a power plant has risen by 26% to €500 million, between 2001 and 2008 and it is likely to increase further [2]. In the United Kingdom, the Government’s financial provision for decommissioning rose from an estimated £2 million in 1970 to £67.5 billion by 2013 [3]. It is clear that decommissioning can sometimes be much more expensive than originally budgeted [4].

Decommissioning is not simply demolition. It is the systematic deconstruction of contaminated, complex nuclear facilities with many large components such as the reactor vessel, steam generator, heat exchangers, pumps, tanks and supporting systems including thousands of meters of pipes – along with even greater volumes of construction materials. Although 99% of the radioactivity is associated with the fuel and the reactor vessel, which is removed following permanent shutdown and requires special attention, significantly large infrastructures remain. Deconstruction of these medium to low level waste requires considerable time and funding, detailed planning and precise execution. It also requires a similar degree of expertise and regulatory control. A critical aspect of decommissioning is that dismantling needs to be carried out in such a way that radioactive and non-radioactive materials are separated. This minimises the amount of waste that will require specialised handling and treatment. Controlled and selective separation also maximises that amount of metallic materials that can be recycled, as well as the amount of concrete rubble that can be reused on site.

A key to reducing the volume of contaminated waste is to improve the separation of material during decommissioning. But reconciling this practice with the minimisation of exposure to workers may be difficult. In many cases remotely operated vehicles, manipulator arms and robots can be used to cut waste materials into smaller pieces. Further development of such technologies is invaluable, as they can reduce waste volumes and increase the packing density of radioactive material to be disposed off, through more selective cutting, thus reducing both costs and radiological risks. Future decommissioning of nuclear facilities will make increasing use of non-contact remote cutting techniques, expertise, resources and waste disposal and management facilities. There is a large variety of size reduction/dismantling techniques that use cutting and are currently in use and considered state-of-the-art. They can be grouped into, A) mechanical (sawing, shearing, milling, diamond wire sawing, etc). B) thermal (oxy-fuel, thermic-lance, plasma-arc, laser beam, etc), and C) hydraulic (water jet and abrasive water jet, shears) [5-9]. Some of these techniques are also applicable underwater.
When applied underwater, generally radiation protection is improved, but visibility in the cutting area is reduced [10].

The particle size distribution of the resulting aerosol, dust and the quantity of swarf and dross that can be collected during cutting, depend on various factors: cutting technology, cutting parameters, measurement point, aeration, kind of material, and environmental conditions [11-12]. In view of the wide range of decommissioning tasks, many different cutting techniques have been developed so far to demonstrate their potential use. The characteristics of some main cutting techniques that has the potential to be used in dismantling applications are highlighted in Table 1.

<table>
<thead>
<tr>
<th>Technique</th>
<th>Oxy-fuel</th>
<th>Lance</th>
<th>Plasma</th>
<th>Laser</th>
<th>Water-Jet</th>
<th>Mechanical</th>
</tr>
</thead>
<tbody>
<tr>
<td>Applicability</td>
<td>Low Carbon Alloy steel only</td>
<td>All materials</td>
<td>Only electrical conductive materials</td>
<td>Most materials</td>
<td>Most materials</td>
<td>All materials</td>
</tr>
<tr>
<td>Max. Cutting thickness</td>
<td>&gt; 2000mm</td>
<td>2000mm</td>
<td>170mm</td>
<td>110mm</td>
<td>150mm</td>
<td>&gt; 2000mm</td>
</tr>
<tr>
<td>Secondary Emission</td>
<td>Hot oxide, fumes, aerosols</td>
<td>Gaseous, dust and solid products</td>
<td>Gaseous, dust and solid products</td>
<td>Gaseous, dust and solid products</td>
<td>Abrasive, fluid product and dust</td>
<td>Scraps, burrs, dust</td>
</tr>
<tr>
<td>Underwater Cutting</td>
<td>Yes (poor performance)</td>
<td>Yes (dry activation required)</td>
<td>Yes</td>
<td>Yes</td>
<td>Yes</td>
<td>Yes</td>
</tr>
<tr>
<td>Contact force</td>
<td>Negligible</td>
<td>Low</td>
<td>Negligible</td>
<td>Negligible</td>
<td>Medium</td>
<td>High</td>
</tr>
<tr>
<td>Standoff tolerance</td>
<td>Low</td>
<td>Low</td>
<td>Low</td>
<td>High</td>
<td>Low</td>
<td>None</td>
</tr>
<tr>
<td>System Cost</td>
<td>Low</td>
<td>Low</td>
<td>Medium</td>
<td>Medium - High</td>
<td>High</td>
<td>Medium - High</td>
</tr>
<tr>
<td>Remote Operation</td>
<td>Yes</td>
<td>Difficult</td>
<td>Yes</td>
<td>Yes</td>
<td>Yes</td>
<td>Yes</td>
</tr>
<tr>
<td>Specific Hazard</td>
<td>Preheat flame and hot oxides</td>
<td>High gaseous byproducts and fumes</td>
<td>Electrical, brightness, gas and fumes</td>
<td>Laser beam, and fumes</td>
<td>Secondary treatment of Effluent</td>
<td>Noise and Vibration</td>
</tr>
<tr>
<td>Observation</td>
<td>The oxygen flow is critical</td>
<td>Difficult in remote operation and secondary emission management</td>
<td>Need high purity gas and can only cut selected material and geometries</td>
<td>Flexible, high automation, excellent selective size reduction capability</td>
<td>Costly post clean up operation</td>
<td>Need high electrical power supply</td>
</tr>
</tbody>
</table>

Each cutting technique has its advantages and disadvantages. However, at present contractors mostly use mechanical techniques, because they have abundant knowledge and experience in using these tools. Nevertheless, as complexity, urgency and cost of dealing with ever increasing challenges of decommissioning increases, organisations responsible for decommissioning operations are looking for more innovative techniques to deal with the problem. What is needed is a highly automated remote technology that can: deliver a non
contact smarter dismantling process, cut most materials, cut complicated structural geometries, produce minimum secondary emissions, deliver high throughput at large operating distances, require minimum deployment effort and maintenance, be flexible enough to be reused in many decommissioning processes.

Laser cutting is one such technology that meets majority of these decommissioning requirements. In the past, various high power lasers have been used to demonstrate cutting of thick-section metallic materials for nuclear decommissioning applications where constant power density and nozzle standoff distance to the substrate were usually maintained. These included CO$_2$, CO, COIL and Nd:YAG lasers [13]. These lasers can also be used in other decommissioning applications such as surface cleaning and concrete scabbling [14]. All lasers offer unique capabilities, but the flexibility offered from solid-state lasers, employing optical fibre delivery of the laser power, reduces complexity and risks. Development of high power disc and fibre lasers, coupled with improvement in beam delivery, thermal management of the system and multiple channel output, have further enhanced decommissioning capability by providing scalable power in the multi-kilowatt regime with significantly better beam quality [15]. Furthermore, the high value asset, which is the laser itself, can be situated and maintained in a safe clean area, some 100s of meters away, allows the system to be reused for several other decommissioning applications. However, the laser technology has not matured enough to cut extremely thick materials effectively, such as reactor vessel, which requires special consideration. Nevertheless, current laser technology is well capable to cutting material in access of 50 to 60mm in thickness. Significant portion of a nuclear facilities comprise of pressure vessels and dissolvers with wall thickness below the 50mm thickness range and tubes with maximum thickness of 10mm and average diameter of 60mm. It is when used in dismantle these medium to low level waste, the laser technology is likely be the most cost effective cutting technique.

In the cutting of tubes, the biggest challenge encountered for decommissioning, arises due to the profile of the tubes/pipes and their juxtaposition, with respect to each other [16]. They could be bundled, multi-layered, or concentric, in various orientations and sizes. From the deployment consideration, all cutting techniques will have to face this scenario where conventional laser cutting around the tube becomes almost impossible. Therefore, a method of single sided tube cutting needs to be developed. Unlike conventional laser cutting of flat plates or orbital laser cutting of tubes, where the beam focus and the nozzle standoff distance is maintained constant with respect to the tube surface [17], in the single-sided laser cutting described here, both the laser focus diameter and the standoff distance vary relative to the tube surface in one plane. Schematics of the process set up and the laser cutting head used in the tube cutting trials are shown in Figure 1.

Since 2009, TWI Ltd demonstrated applicability, flexibility and enhanced process performance by using high brightness fibre delivered lasers in decommissioning applications. In this paper the potential of using fibre delivered laser technology in decommissioning tasks is highlighted, with emphasis on achieving material separation, particularly for stainless steel tubes, using single and double-sided cutting techniques developed in house.
2 Methodology

Single-sided laser cutting trials on 316L stainless steel tubes were performed using a 5kW multi-mode (MM) fibre laser with a beam parameter product (BPP) of 6mm.mrad. The beam from the laser was focused to approximately 420μm, by using collimating and focusing optics, coaxially aligned with a tailored lens design and a cutting nozzle assembly designed to operate at pressure of 8bar. Table 2 provides details of the equipment and parameters used to perform the laser cutting operations.

Table 2. Equipment and parameters used in single-sided laser tube cutting trials.

<p>| | |</p>
<table>
<thead>
<tr>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Laser, Power and Wavelength</td>
<td>4.8kW max. (BPP ~ 6), λ = 1070 – 180nm</td>
</tr>
<tr>
<td>Fibre Core diameter</td>
<td>0.15 mm</td>
</tr>
<tr>
<td>Collimator focal length</td>
<td>120 mm</td>
</tr>
<tr>
<td>Optical focal length</td>
<td>500 mm</td>
</tr>
<tr>
<td>Tube diameters</td>
<td>60, 155 &amp; 170 mm</td>
</tr>
<tr>
<td>Tube thickness</td>
<td>1.5 to 11.1 mm</td>
</tr>
<tr>
<td>Gas pressure (compressed air)</td>
<td>2 to 8bar</td>
</tr>
<tr>
<td>Nozzle diameter</td>
<td>3.25mm</td>
</tr>
<tr>
<td>Max. Cutting speeds</td>
<td>10 to 2000 mm/min</td>
</tr>
</tbody>
</table>

Laser cutting trials for a given laser power, gas pressure and cutting speed, were performed by traversing the laser beam across the tube in a straight line, while maintaining the focal position along the centre of all tubes. Laser cutting on tubes with different diameters was achieved by extending or reducing the nozzle position, but always keeping the minimum standoff distance (Figure 1) of 10mm and focal position in the centre.

Single and double pass cutting techniques were examined. Maximum cutting speeds reported here were for a complete severing of the tube. Most of the time, if there was any lack of separation, it was encountered at the sides of the tube. At these positions not only the standoff distance, but also the material thickness is at a maximum. Table 3 shows estimated maximum material thickness for particular tube and wall thickness combinations. In addition to laser
cutting of tubes, several other metallic support structures that could be found inside a contaminated nuclear plant, were also cut with the same equipment.

\textit{Table 3. Estimated Max. material thickness for various tube diameters and wall thickness.}

<table>
<thead>
<tr>
<th>Tube diameter, (D_p) (mm)</th>
<th>Tube wall thickness, (T) (mm)</th>
<th>Max. cut thickness, (H) (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>60</td>
<td>1.5</td>
<td>18.73</td>
</tr>
<tr>
<td>60</td>
<td>4</td>
<td>30</td>
</tr>
<tr>
<td>60</td>
<td>5.44</td>
<td>34.5</td>
</tr>
<tr>
<td>60</td>
<td>8.71</td>
<td>42.3</td>
</tr>
<tr>
<td>60</td>
<td>11.1</td>
<td>46.3</td>
</tr>
<tr>
<td>155</td>
<td>1.5</td>
<td>30.4</td>
</tr>
<tr>
<td>170</td>
<td>7</td>
<td>67.6</td>
</tr>
</tbody>
</table>

3 Results and Discussion

\textbf{Tube cutting}

It is clearly desirable to cut large diameter steel tubes with just a single pass of the laser beam while traversing in one plane. Therefore, several simple linear cutting techniques were addressed and it was found that with 5kW of laser power, for a single pass technique, the maximum cutting speed was very slow and complete separation of the tube was limited to tube diameters of the order 60mm and wall thickness of 1.5mm. A double pass technique produced the best results, enabling higher cutting speeds and cleaner cut surfaces. The double pass cutting was initiated and terminated at the centre of the tube, as shown in Figure 1. In one series of experiments, samples were produced with a constant laser power, for each particular tube diameter and wall thickness, by varying cutting speed, standoff distance and gas pressure, to determine the maximum cutting speed for separation. It should be pointed out that for decommissioning purposes, cut quality is not important as long as the component ends up in two pieces. Figure 2 shows the maximum cutting speed for a 155mm diameter tube with 1.5mm wall thickness, at various nozzle gas pressures and laser power settings. The focal position of the laser beam was fixed, as shown in Figure 1. The standoff distance between the nozzle exit and the closest approach to the tube was maintained at 10mm. Pressure dependent laser cutting trials were performed with a constant laser power of 4.8kW and laser power dependent cutting trials were performed at a constant gas pressure of 8bar.

It can be seen that the maximum cutting speed is proportional to both the laser power and the gas pressure. However, there appears to be a higher dependency on the laser power. This is to be expected due to the significant variation in the available laser power density at the top and the bottom edges of a large diameter tube and it is the lower edge of the tube which is more susceptible to adhering dross for variations in both the laser power density and the gas pressure. The maximum cutting speed, was attained with higher laser power, for the same gas pressure. Similarly, the cut quality, in terms of speed, was also better with higher gas pressure for the same laser power.
Figure 2. Laser cutting characteristics of a 155mm diameter tube with 1.5mm wall thickness. Double pass cutting.

Trials on 60mm diameter tubes of various wall thicknesses, with a constant laser power of 4.8kW were also carried out, to determine the effect of assist gas pressure. As for the 155mm diameter tube, the focal position of the laser beam was fixed as shown in Figure 1, using a different nozzle extension tube and the standoff distance was again maintained at 10mm. The maximum cutting speed obtained for each wall thickness is shown in Figure 3.

As would be expected, the smaller the tube wall thickness, the faster the cutting speed but this reduces exponentially with an increase in the tube wall thickness. In all cases, it was noticed that the edge quality in the lower half of all tubes cut was always poorer than in the upper section and also became progressively worse with an increase in the tube wall thickness.

Figure 3. Maximum cutting speeds for a 60mm diameter tube with various wall thicknesses. Double pass cutting.
**Selective Dismantling**  
Single-sided laser tube cutting methods were also developed for selectively removing sections of much larger tubes, and a demonstration was also made to simulate the effectiveness of remote deployment of this technology, by sectioning various pipes of different sizes, wall thickness and orientations, in one continuous robot program. Figure 4 shows the largest diameter tube, at 170mm with a wall thickness of 7mm, used in this work. This was cut with a laser power of 4.8kW, at a linear speed of 100mm/min and using 8bar of compressed air assist gas. In this case a three pass technique was used; the first two cuts removed a segment from the front of the tube, thereby providing more energy at the rear of the tube during the third pass, which produced complete separation. A total cutting time of 7min was required for this tube.

<table>
<thead>
<tr>
<th>Figure 4. Different cutting strategies allowing removal of sections and cutting at angles not perpendicular to the tube axis.</th>
</tr>
</thead>
<tbody>
<tr>
<td>Segmentation of 170mm diameter tube with wall thickness of 7mm</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Figure 5. Demonstration of the capability of laser cutting for decommissioning applications relevant to commonly used industrial structural components.</th>
</tr>
</thead>
<tbody>
<tr>
<td>Selective dismantling of 600mm diameter pipe with wall thickness of 25mm</td>
</tr>
<tr>
<td>Seperation of a 87mm thick structural concrete slab</td>
</tr>
</tbody>
</table>
Sections of tube can be removed easily (should access into the interior be required) and tubes can be cut with the beam incident (at least), up to 45 degrees to the tube axis. Indeed, using the current equipment, it is also possible to cut arrangements of concentric tubes and tube bundles. Selective laser dismantling techniques were also applied to several other materials which might be found during decommissioning of a nuclear power plant. Structures such as large pressure vessels, I, L and T-beams for support structures, thick plates and concrete slabs, have been laser cut. Figure 5 shows these selectively laser cut samples.

**Tube cutting demonstration**

Tube networks or “the nuclear jungle” as it is described in nuclear circles, have collections of tubes of various sizes, thicknesses and orientations. In order to simulate real conditions inside a contaminated cell, a collection of tubes of various diameters and orientations were mounted on a support framework. The support framework was designed to be re-used after each demonstration. The back wall of the support structure was constructed from graphite sheets, used to absorb any laser beam propagating past the tubes. Tubes of diameters from 25mm to 155mm, including arrangements of concentric tubes, were fixed to the support frame using the type of fixtures commonly employed in practice. Using a laser power of 4.8kW and an assist gas pressure of 8 bar, all tubes were cut (using the same nozzle assembly) with a single robot program. Over 50 cuts were made on the tubes and the fixtures, to dismantle this tube network in an elapsed time of 15min. Figure 6 shows before and after images of the demonstration exercise.

**Figure 6. Tube cutting demonstrator, before and after.**

“**LaserSnake**“ demonstration

A second, “LaserSnake“ demonstration, highlighted the ability of a snake arm robot armed with a fibre laser cutting tool to maneuver through confined spaces and selectively laser cut components inside a simulated contaminated nuclear cell. A mock-up cell (2.5m x 2.2m x 1.5m) containing a 1m long access aperture (200mm in diameter), a pressure vessel wall (6mm thick) and a subsequent arrangement of pipework, was constructed. Figure 7 shows the system entering the cell through access aperture, avoiding an obstacle and then cutting an access hole in the wall of the pressure vessel. The LaserSnake then enters the pressure vessel
to inspect the pipework and selects, using its on-board vision system, the targets that require cutting, before re-tracing its movements to finally withdraw itself from the cell.

**Figure 7.** LaserSnake demonstration.

**Laser Scabbling demonstration**

The same laser with a change in the optical configuration and using a new processing head, can also be used for selectively removing the surface of contaminated concrete. The process was found to be independent of the attitude of the concrete. Figure 8 is a still image taken from a video sequence showing the system operational in the removal of a 1m x 1m square section of concrete, to a minimum depth of 10mm, using a single pass. Note the effectiveness of the debris removal system. Also seen in this image are the laser stripes used to measure the concrete surface topography, making the system fully automated.

**Figure 8.** Laser scabbling demonstration selectively removing a 1 x 1m section of concrete in single pass.
4 Cost effectiveness of using fibre delivered laser in decommissioning

Cost effectiveness of using fibre delivered laser technology for decommissioning can be due to:

• The capability of a single laser cutting system to cut most materials of many geometrical configurations means reduced equipment deployment and maintenance inside a contaminated cell. This will reduce the decommissioning time and radiological risks to workers.
• The laser cut kerf is narrower than compared with other thermal cutting techniques, allowing faster cutting speed and release of smaller volume fumes and dross in the nuclear cell and ventilation systems. This will reduce post processing cost, reduce filtration change and maintenance, and reduce radiological risks to workers.
• Lighter processing heads with minimum services inside the contaminated cell provides the opportunity for much smaller and simpler manipulation system to be used for deployment. This reduces efforts in deploying the system and the overall cost of the system needed inside the cell.
• The most expensive part of the decommissioning system with respect to fibre delivered laser technology is the laser itself. which is becoming less expensive, These can be located in a clean uncontaminated area and the same system therefore has the potential to be used for many applications. The only disposable parts will be the cutting head and the delivery fibre, which are, compared to the laser, cheap and easily replaced.
• Laser has proven itself to be the most efficient and effective metal cutting technology in the Job-Shop environment. A dedicated decommissioning Chop-Shop can be an effective solution to improve waste packing density, requiring less storage and special construction of storage boxes, thus providing better waste management strategy.

5 Conclusions

A very effective and efficient system for dismantling nuclear grade stainless steel tubes and steel structures, using fibre delivered laser technology has been developed. The cutting head, tailored for these applications, is light, has a significant standoff tolerance, and is relatively simple to deploy and operate remotely. For tube cutting from one side of all tubes of different diameters and wall thicknesses, the most critical regions are the tube sides, which require both higher laser power and assist gas pressure, for clean separation. As a result, a double pass technique was preferred to a single pass method as the optimum laser cutting configuration.

The laser technology has also demonstrated itself to be a sophisticated system for concrete scabbling, compared with other decommissioning technologies; this makes laser a very versatile technology. The selective demonstrations has shown that the processing speeds and flexibility offered by the fibre delivered laser beams to be an effective tool for decommissioning in confined spaces. These qualities associated with the fibre delivered laser beams, can reduce complexity during the deployment process, provide minimum process interruptions, and improve waste management strategy, which all can results in improved safety, and thus reducing decommissioning costs.
6 Acknowledgments

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7 References

EVALUATION OF WEAR PROPERTIES AND APPLICATIONS OF LASER CLADDED MMC COATINGS

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Abstract

In the presented study, laser cladding is used to apply thick MMC (metal matrix composite) coatings on different substrates. Depending on the type of substrate (steel based, inconel 625 or hastelloy C22), different MMC combinations will be tested and optimized in order to achieve a minimal dilution in combination with a metallurgical bonding of the coating with the substrate. The type of substrate determines the type of matrix metal powder used during cladding and hard particles WC/W2C are used to reinforce the matrix. The wear properties of the optimized coatings are evaluated using different tests, namely ball-on-disc tests and ball cratering tests. For high temperature applications, the wear properties will be tested using ball-on-disc up to 500°C. The wear tracks are evaluated using scanning electron microscopy (SEM). Other coating properties such as microstructure and hardness are also studied into detail. The size, shape and concentration of the hard particles are found to have a very important impact on the achieved wear properties. To prove the industrial value of the process, an industrial application is tested in a production facility.

Keywords: laser cladding, MMC, wear resistance

1 Motivation/State of the art

Laser cladding is an additive process wherein a laser source is used to melt metal-based powder or wire on to a metal substrate [1,2]. A laser beam melts the additive material and a thin layer of the substrate. In this way metal or metal matrix composite (MMC) coatings with typically a thickness of the order of 1 mm are formed. Thanks to the superficial melting of the substrate, a strong metallurgical bond is formed between substrate and coating. With a low and local heat input, laser cladding is very well suited for the treatment of heat sensitive materials and components. Deformation of the component is limited and the heat affected zone is small. The high cooling rate during laser treatment results in coatings with a fine microstructure and minimal reaction between the ceramic and metallic phase in case of MMC coatings. The presence of hard ceramic particles results in an improved wear resistance of the coating.
The combination of an optimised chemical composition of the matrix powder together with ceramic particles leads to an improved wear and corrosion resistance. After deposition, machining of the component to its final dimensions is mostly required.

As laser source, different types of lasers can be used: CO₂, Nd:YAG, diode, disk or fiber laser. The former two lasers are the most commonly used lasers in materials processing by laser welding and laser cutting. However, there is currently a strong development in new, more compact and more efficient lasers: the diode, disk and fiber laser. The results presented in this paper are obtained using a diode laser as heat source.

As mentioned earlier, the laser cladding process enables the treatment of heat sensitive materials and deformation sensitive components, which cannot be processed by other techniques like conventional welding. The technique is increasingly being used in industry as a pro-active technology for corrosion and wear protection and as a reactive technology for repair of worn components. In both aspects, laser cladding is a technology contributing to cost-effective maintenance. Carbide metal matrix composite materials are known for their high resistance to wear thanks to the combination of properties given by hard phase particles included in a tough matrix. For laser cladding mostly nickel based materials are used as matrix phase.

In the present investigation, coatings of tungsten carbide particles in a nickel based matrix are produced on different material grades. Three types of material are clad with tungsten carbide powder in order to improve the wear resistance. The wear properties of the optimized coatings are evaluated using different tests, namely ball-on-disc tests and ball cratering tests. An industrial application for which a tungsten carbide containing coating was applied is a die for the extrusion of aluminum. The wear properties of the optimized coating is evaluated using both laboratory and industrial production tests.

2 Experimental

The experimental setup uses a 3 kW fiber-coupled diode laser (Laserline), which uses specific optics to create a circular spot with a diameter of about 3.7 mm at the substrate. A powder feed unit of Medicot, which is commonly used for thermal spraying applications, is used. The powder is supplied in an argon gas flow to the coaxial cladding head. A CCD camera through a semi-transparent mirror coaxial with the laser beam, enables the alignment of the cladding head to the area to be treated.

2.1 Feedstock powder

The first matrix powder (Ni1540 from Hoganas) consists of a nickel based alloy with following chemical composition: Ni (bal.) – 7.5Cr – 1.6B – 3.5Si – 0.3C – 2.6Fe. The second matrix powder (Nistelle C22 from Deloro) consists of a nickel based alloy with following chemical composition: Ni (bal.) – 21.5Cr – 13.5Mo – 3W – 4Fe.

Table 1: Overview of the feedstock powders used in the study

<table>
<thead>
<tr>
<th>Feedstock</th>
<th>Type</th>
<th>Supplier</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ni1540</td>
<td>NiCRBSi</td>
<td>Hoganas</td>
</tr>
<tr>
<td>Nistelle C22</td>
<td>Hastelloy</td>
<td>Deloro</td>
</tr>
<tr>
<td>In625</td>
<td>Inconel</td>
<td>Hoganas</td>
</tr>
</tbody>
</table>
The third matrix powder (In625 from Hoganas) consists of a nickel based alloy with following chemical composition: Ni (bal.) - 21.5Cr – 9Mo – 3.8Nb – 1.4Fe – 0.4Si. An overview is given in Table 1. The carbides used in all coatings are spherical tungsten carbides from C&M Technologies, in the size range of 45-125µm.

2.2 Substrate material

The type of substrate chosen for the different cases depends on the final application. Both the Inconel and Hastelloy will be applied on a stainless steel substrate (AISI316, standard number 1.4404), the Ni1540 coating will be applied on a hot work tool steel (H11, standard number 1.2343).

2.3 Coatings

Depending on the chemical composition/carbon equivalent of the substrates, there will be a large difference in pre-treatment and cladding parameters in order to obtain a crack free coating for each coating/application. For each coating a maximal amount of WC in the cladded layer is targeted, since the reduction in wear is essential. Concerning the industrial application, the extrusion die only needs one layer with a high WC content.

2.4 Wear testing equipment

Two types of wear testing have been performed on the laser cladded samples. Both a conventional ball-on-disc test for dry sliding wear and a mild abrasive ball cratering test have been performed. Before testing the coatings have been grinded and polished in order to have a smooth surface with lower roughness. In most industrial applications the coatings are also subjected to a grinding step.

Ball-on-disc test

In the ball-on-disc test (Wazau TRM 1000) an Al₂O₃ ball with a diameter of 5.4 mm was used as counterbody. The hardness of the ball is approximately 2000 HV. A sliding speed of 28 m/min under a load of 100/200 N was used for all tests. The test is performed at elevated temperature (400°C). After the test, the wear volume of the wear track has been evaluated using white light profilometry measurements. Abrasive wear of laser cladded Ni based tungsten MMC coatings was studied by Van Acker et al. [3].

Ball cratering test

The mild abrasive wear resistance of the coatings has been evaluated using a micro scale abrasion or ball cratering test [4,5]. The principle of the test is sketched in figure 1. A ball is gradually abraded in the sample by 4 µm SiC particles, entrained in the contact zone between a rotating ball and the sample. The particles are suspended in deionized water with a concentration of 20 vol% particles and continuously dripped into the contact. The rotational speed of the 25 mm diameter 100Cr6 ball was 76 rpm and the load was 0.2N.

Figure 1. Principle of the ball cratering test
3 Results and Discussion

Depending on the type of substrate the laser cladding parameters were adjusted in order to obtain a crack free coating. The high carbon equivalent of the H11 steel substrate requires a high preheat temperature, so the extrusion die was preheated above 500°C. The austenitic stainless steel was only preheated when applying layers with high amount of carbides. All the laser cladding parameters will be discussed in detail. For each coating the wear resistance will be evaluated.

3.1 Applied laser coatings

For each coating combination the process parameters were optimized. For each coating a maximal amount of WC in the cladded layer is targeted, since the reduction in wear is essential. In the next table (table 2) an overview of optimized process parameters and coatings is shown. Flow of both matrix and ceramic powder will be adjusted in such a way that the final layer thickness is about 1 mm.

Table 2: Overview of the optimised process parameters and coatings

<table>
<thead>
<tr>
<th>Number</th>
<th>Composition</th>
<th>Clad speed [mm/min]</th>
<th>Laser power [W]</th>
<th>Pre-heat [°C]</th>
<th>Layer thickness [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>120801</td>
<td>C22 pure</td>
<td>1000</td>
<td>1800</td>
<td>-</td>
<td>0.95</td>
</tr>
<tr>
<td>120802</td>
<td>C22 + 20 vol% WC</td>
<td>1000</td>
<td>1500</td>
<td>-</td>
<td>0.9</td>
</tr>
<tr>
<td>1205114</td>
<td>C22 + 40 vol% WC</td>
<td>500</td>
<td>700</td>
<td>400</td>
<td>0.95</td>
</tr>
<tr>
<td>120803</td>
<td>Inc. 625</td>
<td>1000</td>
<td>1800</td>
<td>-</td>
<td>1</td>
</tr>
<tr>
<td>120804</td>
<td>Inc. 625 + 15 vol% WC</td>
<td>1000</td>
<td>1400</td>
<td>-</td>
<td>0.95</td>
</tr>
<tr>
<td>1205112</td>
<td>Inc. 625 + 30 vol% WC</td>
<td>500</td>
<td>800</td>
<td>400</td>
<td>0.9</td>
</tr>
<tr>
<td>1205110</td>
<td>Inc. 625 + 40 vol% WC</td>
<td>500</td>
<td>800</td>
<td>400</td>
<td>0.9</td>
</tr>
<tr>
<td>110105</td>
<td>Ni1540 + 50 vol% WC</td>
<td>500</td>
<td>700</td>
<td>500</td>
<td>1</td>
</tr>
</tbody>
</table>

3.2 Cross sections

In the next figures cross sections are shown of the optimized laser cladded coatings. As can be seen from these figures the coatings always have a minimal dilution with the substrate but have a good metallurgical bonding. All coatings are absolutely crack free.

Hastelloy C22 based coatings:

Figure 2. Cross sections of optimised hastelloy C22 coatings
Inconel 625 based coatings:

![Cross sections of optimised inconel 625 coatings](image)

**Figure 3. Cross sections of optimised inconel 625 coatings**

Ni1540 + 50 vol% WC coating:

![Cross sections of optimised Ni1540 + 50 vol% WC coating](image)

**Figure 4. Cross sections of optimised Ni1540 + 50 vol% WC coating**

3.3 Wear tests

As mentioned in paragraph 2.4 two types of wear testing has been performed on the laser cladded layers. For both the hastelloy based and inconel based coatings two tests have been performed. The Ni1540 based coating was subjected to the ball on disc test and since this coating was applied for an industrial application, this coating was also tested under industrial extrusion conditions. The results of the wear tests are as follows:

**Ball on disc tests**

**Table 3. Ball on disc wear coefficient of hastelloy C22 based coatings**

<table>
<thead>
<tr>
<th>Coating</th>
<th>Load [N]</th>
<th>Temperature [°C]</th>
<th>Wear coefficient [mm³/Nm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>C22</td>
<td>100</td>
<td>400</td>
<td>46.6</td>
</tr>
<tr>
<td>C22</td>
<td>200</td>
<td>400</td>
<td>30.6</td>
</tr>
<tr>
<td>C22 + 20 vol% WC</td>
<td>100</td>
<td>400</td>
<td>7.9</td>
</tr>
<tr>
<td>C22 + 20 vol% WC</td>
<td>200</td>
<td>400</td>
<td>9.5</td>
</tr>
<tr>
<td>C22 + 40 vol% WC</td>
<td>100</td>
<td>400</td>
<td>2.32</td>
</tr>
<tr>
<td>C22 + 40 vol% WC</td>
<td>200</td>
<td>400</td>
<td>2.44</td>
</tr>
</tbody>
</table>

From table 3 it can be seen that the wear resistance of the applied coating increases with an increasing amount of tungsten carbides. The decrease of the wear coefficient with
increasing amount of carbides is logarithmically, so a very strong influence of the amount of carbides is found.

**Table 4. Ball on disc wear coefficient of inconel 625 based coatings**

<table>
<thead>
<tr>
<th>Coating</th>
<th>Load [N]</th>
<th>Temperature [°C]</th>
<th>Wear coefficient [mm³/Nm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>In625</td>
<td>100</td>
<td>400</td>
<td>35</td>
</tr>
<tr>
<td>In625</td>
<td>200</td>
<td>400</td>
<td>29.7</td>
</tr>
<tr>
<td>In625 + 15 vol% WC</td>
<td>100</td>
<td>400</td>
<td>14.8</td>
</tr>
<tr>
<td>In625 + 15 vol% WC</td>
<td>200</td>
<td>400</td>
<td>15.7</td>
</tr>
<tr>
<td>In625 + 30 vol% WC</td>
<td>100</td>
<td>400</td>
<td>4.65</td>
</tr>
<tr>
<td>In625 + 30 vol% WC</td>
<td>200</td>
<td>400</td>
<td>4.15</td>
</tr>
<tr>
<td>In625 + 40 vol% WC</td>
<td>100</td>
<td>400</td>
<td>1.25</td>
</tr>
<tr>
<td>In625 + 40 vol% WC</td>
<td>200</td>
<td>400</td>
<td>2.8</td>
</tr>
</tbody>
</table>

From table 4 it can be seen that the wear resistance of the applied coating increases with an increasing amount of tungsten carbides. The decrease of the wear coefficient with increasing amount of carbides is logarithmically, so a very strong influence of the amount of carbides is found comparable with the results for the hastelloy C22 based coating.

The wear resistance of a Ni1540 + 50 vol% WC coating is compared with a nitrided hot work tool steel surface. Since the Ni1540 + WC coating is applied to improve the wear resistance of an aluminum extrusion die, this coating should be compared with the standard solution for this application. Nitriding hot work tool steel in a gaseous environment is by far the most common surface treatment for extrusion dies. It increases the service life substantially by protecting the surface against wear. Nitriding of steel involves diffusion of nitrogen into the surface at temperature ranges from 450 – 580 °C [6]. The treatment generates a 50 – 300 μm thick nitrogen enriched diffusion zone and a 2 – 10 μm thick iron nitride compound layer on top which attains a hardness of 1000 – 1200 HV. This compound layer is relatively brittle and it is not clear that its presence is advantageous for wear resistance. For this reason an alternative solution using a laser cladded Ni1540 + WC coating was studied.

The steel substrate for this application is a H11 hot work tool steel with a carbon equivalent = 1.59. All laser cladding tests performed on hardened H11 steel resulted in cracks in either the coating or the heat affected zone of the cladded area. For this reason it was decided to perform laser cladding on soft H11 steel and harden the tool after cladding. After hardening, the tool is machined to its final dimensions.

**Table 5. Ball on disc wear coefficient of Ni1540 coating compared with nitrided tool steel**

<table>
<thead>
<tr>
<th>Coating</th>
<th>Load [N]</th>
<th>Temperature [°C]</th>
<th>Wear coefficient [mm³/Nm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Nitrided steel</td>
<td>100</td>
<td>400</td>
<td>48.98</td>
</tr>
<tr>
<td>Ni1540 + 50% WC</td>
<td>100</td>
<td>400</td>
<td>8.18</td>
</tr>
<tr>
<td>Nitrided steel</td>
<td>200</td>
<td>400</td>
<td>78.7</td>
</tr>
<tr>
<td>Ni1540 + 50% WC</td>
<td>200</td>
<td>400</td>
<td>6.46</td>
</tr>
</tbody>
</table>

From table 5 it can be seen that the wear resistance of the applied coating is significantly higher than the wear resistance of a nitrided tool steel surface. From these results...
it is expected that the performance of the laser cladded extrusion die will be significantly better compared with the standard nitrided hot work tool steel.

**Ball cratering tests**

*Table 6. Ball cratering wear coefficient of hastelloy C22 and Inconel 625 coatings*

<table>
<thead>
<tr>
<th>Coating</th>
<th>Amount of WC [Vol%]</th>
<th>Load [N]</th>
<th>Wear depth [µm]</th>
<th>Wear coefficient $10^{-3}$ [mm³/Nm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>C22</td>
<td>0</td>
<td>0.2</td>
<td>74</td>
<td>1.41</td>
</tr>
<tr>
<td>C22</td>
<td>20</td>
<td>0.2</td>
<td>57</td>
<td>0.89</td>
</tr>
<tr>
<td>C22</td>
<td>40</td>
<td>0.2</td>
<td>50</td>
<td>0.66</td>
</tr>
<tr>
<td>In625</td>
<td>0</td>
<td>0.2</td>
<td>72</td>
<td>1.32</td>
</tr>
<tr>
<td>In625</td>
<td>15</td>
<td>0.2</td>
<td>67</td>
<td>1.12</td>
</tr>
<tr>
<td>In625</td>
<td>30</td>
<td>0.2</td>
<td>55</td>
<td>0.80</td>
</tr>
<tr>
<td>In625</td>
<td>40</td>
<td>0.2</td>
<td>51</td>
<td>0.68</td>
</tr>
</tbody>
</table>

From table 6 it can be seen that the wear resistance of the applied coating increases with an increasing amount of tungsten carbides. The decrease of the wear coefficient with increasing amount of carbides is linear, so a less strong influence of the amount of carbides is found. From these tests it can be seen that the wear resistance of the laser cladded MMC coatings have a better performance under sliding conditions compared with abrasive conditions. From the morphology of the laser cladded MMC coating this behavior in wear resistance can be expected.

**3.4 Industrial application: aluminum extrusion die**

From the above results it can be seen that laser cladded MMC coatings have a significantly higher wear resistance compared to nitrided hot work tool steel surfaces. To test the wear reduction of the extrusion die, the laser cladded die was tested under real conditions. The service life of the nitrided hot work steel extrusion die is compared with a laser cladded die.

The die chosen for the industrial test is an inner porthole tool for extruding aluminum tube. In the next figure 5 the laser cladding of the die is shown. As can be seen from the picture, the laser cladding was performed with a lateral nozzle. In the same figure the result of the dye check is shown. The cladded track was 100% crack free. The width of the cladded track is about 5 mm and the thickness of the layer is 1 mm. In total 2 extrusion dies were laser cladded. Preheating of the die was performed using induction heating.

*Figure 5. Left: laser cladding extrusion die, right laser cladded track*
The service life of the first laser cladded die was higher than the service life of a nitrided tool. The nitrided steel die had to be re-nitrided 5 times and even then the service life of the laser coated tool could not be reached. The service life of the nitride steel die was 5 (the die was re-nitrided 4 times) times 5000 m of extrusion length (each nitride layer lasts about 5000 m), so in total 25.000 m of extruded tube. The laser cladded die lasted over 38.000 m of extruded tube without reworking of the laser cladded surface. The extrusion speed used with the laser cladded die was 70 m/min compared with the standard extrusion speed of 35 m/min (with the nitride steel die), so productivity was significantly higher. The extrusion process with the laser cladded die had to be stopped because cracks occurred in the bridges of the die, so not because of a failure of the laser cladded layer. This can be seen in the next figure.

![Crack in bridge](image)

**Figure 6. Laser cladded die with cracked bridge**

The service life of the second laser cladded die was over 7 times higher than the service life of a nitrided die (25.000 m of extruded tube). The laser cladded die lasted over 82.000 m of extruded tube. After reworking of the laser cladded surface, the laser cladded die lasted another 100.000 m of extruded tube (so in total 182.000 m of extruded tube). The extrusion process with the second laser cladded die was stopped when the dimensions of the extruded tube were out of range. A cross section of the extrusion surface is shown in the next figure. On the right the aluminum enters the die, on the left the aluminum leaves the die. As can be seen from this cross section the wear of the extrusion die is most severe on the inlet part.

![Cross section](image)

**Figure 7. Cross section of the laser cladded extrusion zone**

4 Conclusions

- Crack-free nickel based tungsten carbide coatings with a carbide concentration up to 50 vol% could be applied on a hot work tool steel die
- In the case of a hot work tool steel, the substrate was preheated up to 500°C in order to obtain a crack free coating
- For all coatings applied, an increased wear resistance is observed
The wear coefficient during sliding contact with an Al₂O₃ counterbody at elevated temperature is significantly lower for the applied laser coatings compared with a nitride steel surface.

The wear resistance of the laser cladded coatings is higher for wear under sliding conditions compared to abrasive wear.

5 Acknowledgements

The authors would like to acknowledge the partners in the aluminum extrusion die cladding project, in particular IWT for funding the project and the company E-MAX for support and the extrusion series.

6 References

MICROHARDNESS OF THE MICROSTRUCTURAL PHASES OF LASER HARDENED STEELS

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Abstract

This study investigated the microhardness and microstructure of different steels hardened by a fibre laser. These samples included rolled steel, quenched and tempered steel, annealed alloyed steel and conventionally through-hardened steel. Microhardness (HV0.01) was measured in martensite, pearlite, ferrite and cementite structures at different depths below the laser-irradiated surface. The microhardness test results were compared with the conventional macrohardness (HV5) results. Increase of carbon content caused higher microhardness of martensite, although this influence was weaker than the effect on macrohardness. The grain size of rolled ferritic-pearlitic steels had distinct effect on surface microhardness. The macrohardness of quenched and tempered steel might be markedly influenced by the homogeneity of alloy contents. In-grain indentation showed that cementite is around 150 HV harder than pearlite. High Cr content may increase the microhardness on the surface and up to a depth of about 0.6 mm. Soft annealed alloyed steels achieved high surface hardness but poor hardened depth despite high alloy content. Dispersed granular pearlite did not affect the microhardness of soft annealed steel. The macrohardness of the base material was close to the microhardness of the softer phase structure. The measured microhardness was about 100-250 HV higher than the macrohardness.

Keywords: steel, laser, hardening, microhardness, grain size, microstructure.

1. Introduction

Laser transformation hardening is a well-known process of producing hard, wear-resistant areas on the workpiece while retaining the bulk material unaffected. A defocused laser beam is usually used to heat up the material surface above its austenisation temperature, allowing formation of austenite. The base material surrounding the laser irradiated area acts as a heat sink, causing quick self-quenching and phase transformation to martensite [1,2,3]. Surface hardening of steel has been mostly studied with conventional hardness measurement which typically uses a load of several kilograms. However, metallographic analysis of specific
phases and microstructures frequently demands local hardness measurement within a small scale such as tens of micrometres. Unfortunately, such investigations on phase transformation and microstructural transition of laser surface hardened steel are not much available. A microhardness test device uses a very small load (down to a few grams) and is capable to produce indentations within a few micrometres in diameter, making precise local and even in-grain hardness measurement possible [4,5,6]. Microhardness measurement also provides a basis for quality control of thin metallic material and small parts of precision instruments.

In recent years, diode-pumped fibre laser systems have been quickly developed for industrial applications [7,8,9]. Fibre lasers are expected to be suitable for surface treatment of carbon steels, as the wavelength of radiation produced by laser diodes can be efficiently absorbed by iron-based materials [10,11,12].

This study investigated the microhardness of martensite, pearlite, ferrite and cementite on various laser-irradiated carbon steel samples. Microhardness was measured at the irradiated surface, at different depths of the heat affected zone (HAZ) and in the base material. Macrohardness measurement was done for comparison regarding carbon and alloy content and grain size. Quenched and tempered steels were compared with other materials on the microhardness and homogeneity with respect to the initial microstructures.

2. Experimental

The experiment was done in a work cell consisting of a CNC XY table and a YLR-5000-S fibre laser equipment which produces a laser beam with a wavelength of 1070-1080 nm and a maximum nominal output power of 5 kW. The experiment used a focusing lens with the focal length of 150 mm and an output fibre core diameter of 200 μm. All the tests were done with a constant laser power of 1875 W and a distance off the focus of 80 mm, producing a laser power density of 12735 W·cm⁻². A constant traverse rate of 8.0 mm·s⁻¹ was used. The angle between the optical axis and the surface of the sample was 90 degrees. No shielding gas was used. The samples were air-cooled after the treatment.

The hardening tests were done on low-, medium- and high-carbon steel samples with various alloy contents, grain sizes and initial microstructures as given in Table 1. Sample 1 (case hardening steel) and Sample 3 (as-rolled high-silicon steel) contained ferrite and pearlite with distinct grain boundary. Sample 4 (ball and roller bearing steel) consists of pearlite and cementite grains. Sample 2 (alloyed steel) and Sample 5 (vacuum degassed Cr-Ni-Mo-alloyed mould steel) was composed of tempered martensite structures. Sample 6 (alloyed tool steel) and Sample 7 (hot work die steel) contained ferrite and fine granular pearlite [13]. The ASTM grain size number was measured in Sample 1, 3 and 4 via the mean grain diameter method [14]. As Sample 2, 5, 6 and 7 did not contain distinct grain boundaries in their microstructures, their grain sizes were not available. The surface roughness of all samples, Rₐ, was about 2.5 μm.

The macrohardness (HV5) of the samples was measured with a conventional Vickers Hardness Tester (VHT) using a 5 kg load. A CSM Micro-hardness Tester (MHT) with a 10 g load was used for microhardness (HV0.01) measurement. Five random measurements were done at each of the measured depths in each appropriate phase structure. Besides measurement of martensite, in-grain microhardness measurement was done in pearlitic and ferritic grains of Sample 1, 3 and pearlitic and cement grains of Sample 4. For Sample 2, 5, 6 and 7, the microhardness was measured according to the depth only. The average value was used as the macro- and microhardness at this depth. Hardened depth in this study was defined as the depth where measured macrohardness was above 500 HV below the centre of the laser...
irradiated track. Besides, carbon and alloy content were measured on Sample 2 and Sample 5 with energy dispersive spectroscopy method (EDS) combined with scanning electron microscope (SEM).

<table>
<thead>
<tr>
<th>Sample No:</th>
<th>Grade</th>
<th>Grain size (ASTM)</th>
<th>Delivered condition</th>
<th>Compositions (wt.%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>AISI 4820</td>
<td>10</td>
<td>hot rolled</td>
<td>C</td>
</tr>
<tr>
<td>2</td>
<td>AISI 4340</td>
<td>NA</td>
<td>Q&amp;T</td>
<td>C</td>
</tr>
<tr>
<td>3</td>
<td>Imatra Imamic</td>
<td>8.5</td>
<td>as rolled</td>
<td>C</td>
</tr>
<tr>
<td>4</td>
<td>AISI 5210</td>
<td>7</td>
<td>through hardened</td>
<td>C</td>
</tr>
<tr>
<td>5</td>
<td>AISI P20 mod.</td>
<td>NA</td>
<td>Q&amp;T</td>
<td>C</td>
</tr>
<tr>
<td>6</td>
<td>AISI H13 mod.</td>
<td>NA</td>
<td>soft annealed</td>
<td>C</td>
</tr>
<tr>
<td>7</td>
<td>AISI H10 mod.</td>
<td>NA</td>
<td>soft annealed</td>
<td>C</td>
</tr>
</tbody>
</table>

3. Results and discussion

Fig.1, Fig.3 and Fig.5 contain the measured hardness profiles along the depth below the laser treated surface of the materials. The micro- and macrohardness of various phase structures at different depths are compared. The microscopic view of the microstructures at the surface, in the HAZ and in the base material are shown in Fig.2, Fig.4 and Fig.7.

3.1 Hardness and microstructure of rolled medium-carbon steel and through-hardened high carbon steel

Fig.1 describes the micro- and macrohardness of rolled medium-carbon steels (Sample 1 and 3) and the through-hardened high carbon steel (Sample 4). The corresponding microstructure in different depths of the samples is seen in Fig.2. The surface microstructure of Sample 1, 3 and 4 is fully martensitic as can be noticed in Fig.2(a), (d) and (g). As expected, the surface macrohardness is fully dependent on the carbon content. The macrohardness of Sample 1, 3 and 4 is 505, 704 and 812 HV with 0.20%, 0.36% and 0.95% C respectively (Fig.1(a)). But interestingly, the microhardness of martensite grains in the same structures is not much different. The microhardness on the surface of Sample 1, 3 and 4 is 945, 938 and 1051 HV respectively. The difference in the behaviour between micro- and macrohardness is discussed in more detail in Chapter 3.5. Deeper from the surface, the ferrite-pearlitic structures in Sample 1 and 3 can be distinguished and in Sample 4 pearlite and cementite grains can be seen (Fig.2(c), (f) and (i)).
Fig.1. Hardness profiles along the depth of Sample 1, Sample 3 and Sample 4: (a) Microhardness of non-ferritic structures and macrohardness; (b) Microhardness of ferritic structures. M: martensite, P: pearlite, C: cementite, F: ferrite. (Microhardness: HV0.01; Macrohardness: HV5)

As Fig.1(a) shows, the microhardness of Sample 1, 3 and 4 starts to dramatically decrease between 0.6 mm and 0.8 mm deep, which can be presumed to be the depth of fully martensitic layer. For Sample 1 and 3, partially martensitic structure may be present between 0.6 mm to 1.6 mm, causing gradually reduced microhardness. In Sample 1, pearlite at 0.8 mm deep (HAZ) shows higher microhardness (761 HV ) than the base material (252 HV). This is possibly caused by homogenisation with decreased grain size and can be noticed by comparing Fig.2(b) and (c). Similarly in Sample 3, at the depth of 0.8 mm, pearlite gives markedly high microhardness of around 796 HV compared with the base material (490 HV). Comparison of grain size can be seen in Fig.2(e) and (f). Different from Sample 1 and 3, Sample 4 shows respectively constant microhardness of cementite and pearlite at 0.8 mm deep and beneath. Because of the high carbon content and the presence of ferrite only in the pearlite grains, carbon diffusion may have been largely restrained and original grain size is not much changed. As the in-grain indentation shows, the microhardness of cementite (around 559 HV) is distinctly higher than pearlite (around 413 HV), because cementite consists of pure iron carbide.
As seen in Fig.1(b), the microhardness of ferrite of Sample 1 and 3 at 0.8 mm deep is respectively 376 and 315 HV, which is considerably higher than in the base material (216 HV at 2 mm deep). As exhibited in Fig.2(b) this is likely due to the reduced grain size of ferrite caused by recrystallization. In deeper structures, microstructural grains remain larger since less grain boundaries are formed inside the original ferrite grains (Fig.2(c) and (f)). At the depth of 0.8 mm to 1.6 mm, Sample 1 shows higher microhardness of ferrite than Sample 3, although Sample 3 contains much higher Si content that aids to increase the hardness of ferrite [14]. This is likely due to the markedly larger ferritic grain size of Sample 3 compared with Sample 1 as seen in Fig.2(b) and (e). See the explanations of microhardness measurement in Chapter 3.5.

Fig.1 also indicates that the macrohardness of the base material is close to the microhardness of the softer phase structure. For Sample 1 and 3, the microhardness of ferrite
is close to the macrohardness. For Sample 4, the macrohardness is approximately equal to the microhardness of pearlite. An explanation is the plastic deformation of different structures as macrohardness indentation is done. A load of several kilograms used in Vickers hardness test drives the diamond indenter into the material, causing plastic deformation of a number of micro-grains nearby the produced marker. In ferritic-pearlitic structure, ferrite contains almost pure α-iron and pearlite includes 88% ferrite and 12% cementite. Due to the body-centred-cubic crystal lattice structure of α-iron, ferrite grain is thought to have lower yield point than cementite (Fe3C) that features orthorhombic structure [15]. Thus ferrite grains could be more plastically deformed than pearlite grains and this might significantly affect the measured macrohardness value.

### 3.2 Hardness and microstructure of soft annealed alloyed steel

In Fig.3 the micro- and macro hardness of the soft-annealed Sample 6 and 7 is shown. The corresponding microstructure in different depth of the samples can be seen in Fig.4. Sample 7 shows a bit higher macrohardness on the surface than Sample 6 (Fig.3). The macrohardness values are 647 and 716 HV for Sample 6 and 7 respectively. Yet Sample 6 exhibits drastically higher microhardness than Sample 7 on the surface and till 1.6 mm deep. Consistent microhardness of above 850 HV between the surface and 0.6 mm deep is noticed. These are believed to be caused by the high Cr (4.95%) and Si (0.97%) contents in Sample 6. Fig.4 shows the microstructures of Sample 6 and 7 at the treated surface, at 0.6 mm deep and in the base material. Due to the soft annealing treatment in the manufacturing process, Sample 6 and 7 consist of ultra-fine granular pearlite structure which is dispersed in the ferrite grains and therefore grain boundary is hardly distinguishable. Grain growth can be noticed on the treated surface of both materials, showing visible ferrite boundaries among the formed martensite areas. The transitional layer at the depth of 0.6 mm indicates that carbon diffuses from the fine granular pearlite to the surroundings and causes local homogenisation. A similar heat affected zone to the depth of around 1.8 mm is produced in both samples.
Fig. 4. Microstructural comparison of Sample 6 (etched by 20% Nital) and Sample 7 (etched by 10% Nital) at various depths: (a) Sample 6, surface; (b) Sample 6, 0.6 mm deep; (c) Sample 6, 2 mm deep (base material); (d) Sample 7, surface; (e) Sample 7, 0.6 mm deep; (f) Sample 7, 2 mm deep (base material)

3.3 Hardness and microstructure of quenched and tempered steel

Fig. 5 gives the micro- and macro hardness of quenched and tempered steel (Sample 2 and 5). The corresponding microstructure in different depths of the samples can be seen in Fig. 7. As compared in Fig. 5, Sample 2 shows higher micro- and macrohardness and larger hardened depth although Sample 5 contains similar carbon and higher alloy contents. In order to find an explanation for this discrepancy, energy-dispersive spectroscopy (EDS) was used. The results of EDS element content analysis are shown in Fig. 6. In the base material of Sample 2 and 5, 15 circular areas of 5 μm in diameter were randomly selected and the weight percentages of C, Cr and Mn were measured. The variation of the mass percentage indicates that Cr and Mn are markedly more homogeneous in Sample 2, which is likely due to the process of production by the manufacturer. Fig. 7(a) and (d) also show visible difference on the samples’ irradiated surfaces. It is suspected that the inhomogeneity might have caused the formation of retained austenite in Sample 5. Yet this is not conclusive before X-ray diffraction test of the microstructures is done. The macrohardness profiles exhibit a heat affected depth of around 1 mm on both samples.
**Fig. 5.** Hardness profiles of Sample 2 and Sample 5 along the depth of material (Microhardness: HV0.01; Macrohardness: HV5)

**Fig. 6.** SEM element analysis of C, Cr and Mn on Sample 2 and Sample 5
Fig. 7. Microstructural comparison of Sample 2 and Sample 5 at various depths (etched by 5\% Nital): (a) Sample 2, surface; (b) Sample 2, 0.9 mm deep; (c) Sample 2, 2 mm deep (base material); (d) Sample 5, surface; (e) Sample 5, 0.9 mm deep; (f) Sample 5, 2 mm deep (base material)

3.4 Comparison among Sample 2 (quenched and tempered steel), Sample 3 (rolled steel) and Sample 7 (soft annealed alloyed steel)

Sample 2, 3 and 7 are compared based on Fig.1, Fig.3 and Fig.5. Sample 3 and 7 give similar surface macrohardness of 704 and 718 HV respectively while Sample 2 shows markedly lower surface macrohardness (635 HV). Despite of high alloy content, Sample 7 shows a smaller hardened depth of about 0.4 mm than Sample 3 and 2 (both around 0.9 mm). With similar carbon content, Sample 3 shows the highest surface microhardness. Compared in Fig.2(d) and Fig.4(d), Sample 7 shows some bright grain boundaries on the surface which are suspected to be ferrite. This means large grain size in contrast to Sample 3 that has apparently more homogeneous martensite. The microhardness in the base material of Sample 7 is nearly equivalent to the in-grain microhardness of ferrite in Sample 3, showing that the dispersed granular pearlite has little effect on the microhardness.

3.5 Microhardness (HV0.01) vs. macrohardness (HV5)

The microhardness (HV0.01) results are compared with macrohardness (HV5) of the tested samples in Fig.1, Fig.3 and Fig.5. Comparisons show that measured microhardness values are about 100-250 HV higher than macrohardness. An earlier developed model revealed the influence of indentation size on the measured hardness of crystal materials, as shown in Equ.(1) [16]. This model was later used as a base for microhardness simulations [17]. Comparing indentations using a very small load (e.g. 10 g) and a conventional one (5 kg), \( H_d \) can represent the microhardness and \( H_0 \) the macrohardness. Equ.(1) indicates that the
microhardness is constantly higher than macrohardness.

\[
\frac{H_d}{H_0} = \sqrt{1 + \frac{h^*}{h_i}}
\]  

(1)

where \( H_d \) is the hardness for a given depth of indentation;
\( H_0 \) is the hardness in the limit of infinite depth;
\( h^* \) is a characteristic length depending on the indenter shape, shear modulus and \( H_0 \);
\( h_i \) is the depth of indentation.

The dislocation theory also provides a qualitative explanation for this phenomenon. It is summarized in generally known Petch-Hall relationship, as given in Equ.(2).

\[
R_e = R_i + K \cdot d_g^{-\frac{1}{2}}
\]  

(2)

where \( R_e \) is the yield point;
\( R_i \) is the stress required to make the dislocations move in the grains;
\( K \) is a constant;
\( d_g \) is the grain diameter.

Compared with microhardness test, macrohardness indentation produces a deeper and wider mark that crosses a number of grains. Plastic deformation initiates at some of the sliding dislocations while new dislocations are created as well [18]. Differently, microhardness indentation generates a much smaller marker and thus fewer grains are affected, meaning that fewer slidable dislocations are present as plastic deformation occurs. Therefore, \( R_i \) for microhardness indentation is expected to be higher and \( R_e \) is accordingly higher than that for macrohardness. In in-grain microhardness measurement, since the indentation marker is smaller than the grain size, \( d \) can be regarded as infinitely large and thus in Equ.(2) \( R_e \) is approximately equal to \( R_i \). This means that the yield point for in-grain indentation is basically determined by the properties of the in-grain material. Equ.(2) also indicates that a fine-grained material with a smaller \( d \) may exhibit higher yield point and hardness than a coarse-grained one.

4. Conclusions

This study investigated the microhardness of laser hardened medium-carbon rolled steels, annealed alloyed steels, quenched and tempered steels and a high-carbon through-hardened steel. Microhardness was measured in martensite, pearlite and ferrite structures at different depths below the irradiated material surface. The micro- and macrohardness profiles of the samples were compared. Five random measurements of microhardness were done at each of the measured depths in each appropriate phase structure. The average value was used as the result for discussion.

For rolled ferritic-pearlitic steels, grain size distinctly affected the homogeneity of martensite and the microhardness. Ferrite in the HAZ exhibited higher microhardness due to smaller grain size caused by carbon diffusion. By in-grain indentation, the microhardness of ferrite (around 216 HV), pearlite (around 413 HV) and cementite (around 559 HV) were
obtained. In ferritic-pearlitic and pearlitic-cement steels, the macrohardness of the base material was close to the microhardness of the softer phase structure. The homogeneity of Cr and Mn contents might have significant effect on the macrohardness of tempered martensitic steel.

Rolled steel, quenched and tempered steel and soft annealed alloyed steel of the same carbon content were compared. Rolled steel could achieve higher microhardness of martensite on the surface. Quenched and tempered steel showed similar hardened depth to rolled steel but lower macrohardness on the surface. The micro- and macrohardness of soft annealed alloyed steel were relatively high on the surface but markedly decreased along the depth, producing a relatively small hardened depth. The microhardness of the base material was similar to ferrite, indicating that dispersed granular pearlite had little effect on the microhardness.

In this study, the measured microhardness values were about 100-250 HV higher than macrohardness. This was explained by a model of indentation size effects and the dislocation theory.

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References


STRESS RAISING IN LASER CLAD COMPONENTS DEPENDING ON GEOMETRY AND DEFECTS

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Abstract

The fatigue life of laser clad components is basically determined by stress raisers, which are here studied by Finite Element Analysis. Cylindrical and square section bars are compared for axial, bending and torsional load conditions, which induces the macro-stress field with corresponding stress peaks. Defects from laser cladding such as pores, cracks or the surface roughness superimpose additional stress raising action on this stress field. The geometrical position and orientation of the defects has strong impact on the induced maximum stress level. For surface pores it is demonstrated by the fractography that their occurrence within a critical azimuthal range can initiate fatigue cracking. The different conditions of sample geometry, load situations, materials and defects are compared and discussed. In particular, advanced illustration methods are applied for improved and generalized documentation and explanation of the trends, both qualitatively and quantitatively. Guidelines are presented, in particular emphasizing critical defects and situations, such as surface pores that are difficult to detect because of inclusions, or pores just underneath the surface that generate particularly high stress.

Keywords: laser cladding, defect, geometry, fatigue cracking, stress raiser
1 Introduction

Fatigue cracking of laser clad components takes place from locations of maximum stress. These locations depend on the macroscopic stress load of a certain component under specific load plus local stress raisers, particularly by defects like pores or cracks. A literature survey on defects and fatigue cracking of laser cladding was written by Md. M. Alam [1]. The present study compares four different basic laser clad components and their load case as well as different defects and their location and orientation. Comparison between the different stress raisers is illustrated by various documentation methods. Experimental results from fatigue fractography confirm their origin from stress raisers like pores. While part of the results have already been published,[1-3] we here particularly present results on the fatigue crack propagation dependent on various defects and their locations.

2 Methodology

Four basic laser clad component geometries with corresponding load conditions are studied, namely (i) a cylindrical rod for axial load, (ii) a cylindrical rod for four-point bending load, (iii) a rectangular bar for four-point bending load and (iv) a cylindrical rod for torsional load. The circular rods are laser-clad at the entire surface while the rectangular bar is laser-clad at one side. After laser-cladding, for optimized parameters, the clad surfaces are smoothened by machining off a layer. Visual inspection is carried out for the detection of surface defects, particularly pores. The clad components are fatigue tested, to develop SN-diagrams, also in comparison with non-clad base material samples. Stress analysis by the Finite Element Method, FEM, is carried out to calculate the stress field for the respective load condition of the components, called the macro-field. In addition, local stress analysis of defects is performed, which is an additive stress raiser to the macro-field in its respective location. For certain situations the crack propagation is also calculated by FEA. Fractography by microscopic analysis of the fracture surfaces of the samples after fatigue testing provides additional information. Occasionally, cross sections of the laser clads are made, to study the metallurgy and defects, accompanied by other kinds of analysis, including residual stress measurement. Finally, the main trends are interpreted, categorized and visualized in graphical charts.

2.1 Laser cladding

Laser cladding was carried out at the Technology Centre KETEK Ltd. in Kokkola, Finland, with the parameters shown in Table 1. A diode-pumped Nd:YAG-laser was used. A 1.5 mm thick clad layer was made, machined off then to 0.75 mm. More details can be found in [2,3].

<table>
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<tr>
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<tr>
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<tr>
<td>Gas flow rate</td>
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<td>Lateral clad layer displacement</td>
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</table>
2.2 Fatigue testing

The fatigue testing was carried out at different locations of the Finnish partners. The four different loading situations described above were tested, i.e. axial, bending (cylindrical rod and rectangular bar) and torsional fatigue load. Details about the fatigue testing set-up and first results can be found in [1-3]. As example, Fig. 1(a) shows a cracked cylindrical rod after four-point bending fatigue testing. Beside the primary crack also a secondary crack can be seen. A cross section of the milled laser clad is shown in Fig. 1(b) where the vertical crack (here the secondary crack, denominated E) through the clad layer into the substrate can be seen. Note the wavy interface clad-substrate from the overlapping clad-layers. The crack surface of the rod can be seen in Fig. 1(c). The fatigue testing was stopped after a certain crack propagation and drop in force and strain and the sample was cut through afterwards. In this case it was found that the crack was initiated from a surface pore that was filled with an oxide inclusion, see Figs. 1(d)-(f), which is described in detail in [3].

Fig. 1. (a) 2 cracks at round rod (here non-clad, for comparison) after four-point bending fatigue testing, (b) secondary crack through the clad layer, (c) fracture surface of the rod with pore location as crack initiation, (d)-(f) surface pore and crack surface with ratchet lines. [3]
2.3 FE stress analysis

Stress analysis was carried out at different levels, by Finite Element Analysis, FEA. The static stress field, the macro-field, was calculated for the maximum load of the four cases. For a variety of defect geometries (pores, surface pores, hot cracks, etc.) the stress raiser or stress concentration factor $K$ was calculated locally around the defect. Moreover, the stress raisers were superimposed to the calculated macro-field to study the influence of different locations and orientations of defects in the component. In addition, the crack propagation was calculated (simplified in two dimensions) by FEA, based on the stress intensity $K_I$ at the tip of the crack, leading to the direction and speed of the fatigue crack propagation but also indicating its preferred location of initiation, from maximum stress raisers. In particular, the stress propagation was computed for different locations of initiation, for different sizes and orientations of initiation defects and for different interaction when propagating in the vicinity of defects. Details of the stress analysis method, i.e. the FEA can be found in [1-3]. From the stress analysis, better interpretation of the experimental results has been expected.

Although the study tries to distinguish between the stress field induced by the load including defect stress raisers and the residual stress field as a different mechanism to be optimized, the residual stress field was measured for a few samples and will here be presented.

3 Results and discussion

3.1 Macro stress field depending on load

The calculated macro-stress field for the four cases studied is shown in Fig. 2.

![Fig. 2. Calculated 1st principle macro stress field (in MPa, long section) for (a) axial fatigue loading of a cylindrical laser clad rod, (b) torsional loading (cylindrical), (c) 4-point bend load (cylindrical, here quarter-rod), (d) 4-point bend load (rectangular bar, bottom-clad).[2]
For the axial and torsional load a ‘dog-bone’-geometry was used, following the standards. For axial loading, the representative cylindrical central part shows an almost constant stress value. Torsional load leads to a central (red) peak around the cylinder, rapidly decaying radially inwards, as shear stress slices. For bending load a long area of rather constant maximum stress is obtained at the lower surface, strongly decreasing upwards. This macro field (note: it is the amplitude of the cyclic fatigue load) is the starting condition for the loaded laser-clad component, so far without any imperfections at the surface or in the material. Fatigue cracking is expected to initiate from the respective peak stress location. The material plays here a minor role [2] because the E-modulus of the thin clad and the base material match well.

3.2 Residual stress

As mentioned above, residual stress is present, as a superimposing field, but the central part of the study are the generation of peak stress by the load, geometry, defects and their locations. For some samples the residual stress was measured. Figure 3 shows the measured residual stress (hole drilling method) for the two material combinations studied, at three locations along the laser clad (and then milled) bars, with and without post weld heat treatment (PWHT). Positive tensile stress was measured almost throughout. Therefore residual stress is a critical (superimposing) contribution that will be further studied and improved.

![Residual stress components measured at different locations along the clad (and milled) bar, for the two material combinations with and without post-weld-heat-treatment (PWHT)](image)

3.3 Defects as stress raisers

Figure 4 shows a variety of possible defects from laser cladding, and examples for different locations and orientations of these defects, both for the as-clad surface and when machining off a layer to a smooth surface. From the geometrical nature of the defects it can be distinguished between 0-dimensional defects like pores or inclusions, 1-dimensional defects like the ripples from the as-clad surface or the wavy interface clad-substrate, and defects extended in two dimensions like lack-of-fusion or hot cracks. Photographs of different laser clad defects obtained can be seen in Fig. 5(a)-(i). Figure 5(j),(k) shows the calculated stress field (note: different scale) at a spherical pore, when located just underneath the surface or as surface pore, i.e. representative also for the cracking shown in Fig. 1. More calculated stress fields for laser clad defects as stress raisers and comprehensive analysis can be found in [2,3]. One conclusion is that a pore just underneath the surface is the strongest stress raizer. Moreover, a central location as in Figs. 1,4 is the most critical azimuthal location.
**Fig. 4.** Illustration of the laser clad layer cross section (left as-clad and right after machining) with the variety of possible defects and their positions.

**Fig. 5.** Photographs of laser cladding defects: (a) as-clad wavy surface A and interface B, (b) detection of surface pores F by dye penetration test, (c) surface pore F, (d) surface pore F with oxide inclusion, (e) semi-spherical interface pore J between clad layer and base material, (f) spherical pore I in-clad, (g) irregular inclusion near the interface, (h) hot crack D, (i) interface lack-of-fusion E; (j) local 1st principle stress field (in MPa) calculated for a spherical pore just underneath the surface, G, and (k) the calculated stress field for a surface pore, F, centrally cut-off [2].
The trends of the two overlapping mechanisms, namely the macro-field and the stress raiser defects are visualized in a quantitative manner in Fig. 6 and in a qualitative manner in Fig. 7. In [1-3] the trends are explained and discussed and recommendations are given. Sometimes the size of the defect is of importance, but more often its location or orientation.

**Fig. 6.** Stress concentration factor and stress intensity factor for various clad defects and their geometry variations [2]

**Fig. 7.** Tuning Flow Chart, TFC, describing the combination of different stress raisers of laser clad bars under fatigue load; the arrows indicate lowering of the peak stress [2]
3.4 Crack propagation

The initiation and propagation of a fatigue crack from a stress raiser, for local macro-stress, was calculated by the FEM for various conditions, as shown in Fig. 8, in particular explained in Fig. 8(f), middle. Figure 8(a) shows the significant stress peaks induced by the notches from the long (1D) ripples of as-laser clad surfaces along with an inclined crack (or Lack-of-Fusion) horizontal defect located in the notch of the ripple. Figure 8(b) shows a crack that propagated 1.6 mm from a horizontal crack origin at an as-clad surface notch. The stress peak is around the tip of the vertically propagating crack. From Fig. 8(c) it can be seen that a horizontal crack just 0.1 mm underneath the surface initiates the cracking but propagates to the surface, while a corresponding inclined crack, Fig. 8(d), propagates in both vertical directions, i.e. the situation is very sensitive to the location and orientation of the defect, often more sensitive than to the size of the defect. Figure 8(e) shows that this bidirectional vertical crack propagation can be expected even for a horizontal crack in the clad-substrate interface, and similarly for a tilted crack origin, see Fig. 8(f). Owing to the here applied horizontal stress as the local impact from the macro-field, even horizontally oriented defects rapidly change their direction to vertical.

Corresponding graphs showing the propagating crack length as a function of the crack tip stress intensity $K_I$ in comparison between horizontal and tilted crack origins are shown in Fig. 9(a)-(c). As can be expected, the tilted crack origin causes a slightly higher stress intensity factor, which corresponds to faster propagation and shorter fatigue life. Figure 9(d) shows the kink angle of the propagating crack as a function of the crack length, for different crack (lack-of-fusion) orientations. An attenuated zig-zag-propagation can be seen.

A crack propagating from the top surface can interact with defects beside the propagation crack, deflecting the crack path and changing its speed. Figure 10(a) shows the undisturbed propagation of a crack while Figs. 10(b)-(d) show how the crack is deviated when one or two pores are located close to the crack path. Note that the calculations were carried out in two dimensions for the sake of low computation time, i.e. corresponding to cylindrical rather than spherical defects. The basic trends are the same, although quantitatively weaker for spheres, depending also on the in-plane coordinate location.

The crack initiation length at the surface was 10 $\mu$m, the pore started 100 $\mu$m below the surface, with a diameter of 60 $\mu$m. The distance between the two pores was 100 $\mu$m, once oriented horizontally, once 45º inclined down from the first pore. Axial loading conditions were applied, inducing horizontal tensile stress.

Figure 11 shows the corresponding length of the propagating crack as a function of the number of cycles. As can be seen, compared to a defect-free sample the lifetime is reduced for one defect and even further for a second defect, in different manner depending on their orientation. Again, for the quantitative values the simplification to two dimensions has to be kept in mind. The defects act as local stress raisers that accelerate the crack propagation. Same as here for round defects (pores) and laser cladding, this was already found earlier for lack-of-fusion (note: a two-dimensional planar defect, therefore 2D-analysis was representative) in laser-arc hybrid welding,[1] where it however was shown that the stress field induced by the defect rapidly decayed in space, keeping the interaction in form of acceleration limited (about 10% reduction of lifetime), while other aspects were more important.
Fig. 8. Local defect (left column), crack initiation path (middle or right) and stress field (right, in colour) by FE-calculation: (a) 0.1 mm short tilted crack at the as-clad surface notch, (b) crack that propagated 1.6 mm from a horizontal 0.1 mm crack, (c) horizontal crack close to the surface and its propagation, (d) inclined crack close to the surface and its propagation, (e) horizontal crack at the clad-substrate interface, (f) inclined crack at clad-substrate interface
Crack length propagation as a function of the stress intensity $K_I$ at the crack tip, comparing horizontal to tilted crack origins, see also Fig. 8: (a) initial crack at an as-clad top notch, (b) initial crack 0.1 mm below the top, (c) initial crack at the solid-liquid interface, (d) kink angle as a function of crack propagation length, for three angle of defect-inclination

Fig. 10. Calculated crack propagation path from the top surface: (a) without defect, (b) beside a pore in the clad layer, between two pores at same depth in the clad layer, between two pores of different depth (45° orientation)
4 Conclusions

For the theoretical study of stress raising and fatigue life in laser clad components the following conclusions can be drawn:

(i) The maximum stress in a laser-clad component, which is likely to initiate fatigue cracking, is generated from a superposition of:
   a. the macro-stress field (from load and component geometry),
   b. stress raisers by defects (from laser cladding),
   c. residual stress (generated during laser cladding).

(ii) Mainly tensile residual stress was measured, which can reduce the fatigue lifetime.

(iii) For defects, their location and orientation in the macro-field is often more important for their generation of stress raisers than the defect size.

(iv) Experimental evidence was found where a surface pore has initiated fatigue cracking.

(v) When a crack propagates in the vicinity of a defect it will be locally deflected and accelerated, in general lowering fatigue life; however, the defect is not wide-ranging.

(vi) The visualizing maps developed enable to systematically extend existing knowledge and they facilitate the cognition of knowledge, here for stress raisers through defects.

5 Acknowledgements

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6 References


DESIGN OF MEASUREMENT EQUIPMENT FOR HIGH POWER LASER BEAM SHAPES

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Abstract

To analyse advanced high power beam patterns, a method, which is capable of analysing the intensity distribution in 3D is needed. Further a measuring of scattered light in the same system is preferred. This requires a high signal to noise ratio.

Such a system can be realised by a CCD-chip implemented in a camera system. Most available CCD-based systems do however suffer from a low maximum intensity threshold. Therefore attenuation is needed.

This paper describes the construction of such a beam analysing system where beam patterns produced by single mode fiber laser on a diffractive optical element can be evaluated using a CCD based camera.

The system is tested with various DOE’s for evaluation of efficiency and measurement of scattered light with success. Also tests with capturing beam caustics of focused laser beams from which beam parameters has been fitted and compared with measurements by a commercial product has been done.

The realised system might suffer from some thermal drift at high power; future work is to clarify this.

Keywords: Laser caustic, camera, high power high brightness, laser beam diagnostics

1 Background

In recent years high power high brightness fiber lasers has emerged. These has led to ideas about reshaping the beam into application specific patterns for cutting [1], welding [2] [3] and other macro laser processes.[12]

IPU and Aalborg University has two on-going projects, which utilise a 3 kW single mode fiber laser and beam shaping using Diffractive Optical Elements (DOE’s). [1]

The system should be able to verify beam patterns produced by DOE’s, evaluate scattered light and perform measurements of beam parameters, such as diameter and the beam quality factor. Furthermore flexibility should be considered, so that the system is open and allows access to raw data and automation.
Table 1. Minimum demands for the system.
*All dB values are dB (volts) = 20 x log10 (measuring-ratio)*

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<tr>
<td>Flexibility</td>
<td>High / open system</td>
</tr>
</tbody>
</table>

*Beam pattern assumed to be up to 8 times larger than a round beam

In table 1 is shown the minimum requirements for the system. The numbers are based on design considerations for the requirements for the constructed beam patterns, and the theoretical minimum beam diameter that can be formed with the available equipment. ISO 11146-1:2005 specifies that any detail to be measured in the design should be resolved by minimum 20 pixels in any direction [9]. The system should also be able to measure the intensity distribution in different planes within +/- 2 times the Rayleigh length. [9]

2 Commercial systems

To fulfil the requirements a product search in the market were performed. In table 2 is shown a small amount of available systems and techniques. Based on this market/product analysis, it was decided to produce a “low cost” system, where beam is attenuated before imaged on a camera chip, instead of buying a turnkey system.

It is not expected, that the produced system will be better than any system, that can be bought; it is expected to save some money and gain some flexibility in the final design compared to the commercial systems.
Table 2. Some of the different systems and principles considered

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<td>0.022 μW/mm²</td>
<td>-</td>
<td>32 μW/mm²</td>
</tr>
<tr>
<td>Max power</td>
<td>10 kW</td>
<td>&gt;10 kW</td>
<td>~1 mW</td>
<td>~1 mW</td>
<td>~100 mW</td>
</tr>
<tr>
<td>Resolution</td>
<td>~1 x 1 μm</td>
<td>~8 x 8 μm</td>
<td>4.4 x 4.4 μm</td>
<td>4.7 x 4.7 μm</td>
<td>100 x 100 μm</td>
</tr>
<tr>
<td>Scan area</td>
<td>8 x 8 mm</td>
<td>8 x 8 mm</td>
<td>7.1 x 6.3 x 4.8 mm</td>
<td>12.4 x 12.4 mm</td>
<td>5.4 mm</td>
</tr>
<tr>
<td>Price</td>
<td>Very high</td>
<td>High</td>
<td>Medium</td>
<td>Medium</td>
<td>Unknown</td>
</tr>
</tbody>
</table>

On the market there exist several systems capable of analysing laser beams, and beam patterns [12]. Most of these systems have a low maximum beam intensity threshold compared to the intensity in focus by a 3 kW single mode fiber laser.

One of the few products capable of performing the measurements directly in focus with a 3 kW SM-fiber laser is the Primes FocusSpotMonitor. But the Primes FocusSpotMonitor still has two problems: it has a limited resolution, which is to large compared to the specification in table 1, and it is quite expensive. [4]

Another option is to split the beam and only perform the measurement on a fraction of the beam. One of the systems, which operate with this technique, is Primes HighPower MicroSpotMonitor. [3] In this system, there is an option to add a lens after attenuation which increases the beam, before it is imaged on a CCD chip. This increases the resolution and combines it with the ability to measure high power and high intensities. The product is however quite expensive and suffers from some thermal drift. [4].

The two camera systems are in many ways similar to the Primes HighPower MSM. They are just OEM products with software to perform some measurements.

Pyrocam is also a pixelbased measurements system. However the way to capture data is different, as it is the heat which is measured. Pixels are much bigger and the dynamic range is on 43 dB, which gives a signal to noise ratio of 141. This would be way too little for detecting scattered light. The Pyrocam has it largest advantages spectral areas where tradition CCD/CMOS in various design don’t have any response.
3. System design

First step in the construction of a measurement system is to decide on a measuring principle. Following principles were considered:
- Scanning aperture techniques (integral measurements)
- A rotating needle
- Thermal chips
- CMOS / CCD chips

The scanning aperture technique can also do measurements with micron resolution and excellent signal to noise ratio. The technique were considered and abandoned, due to difficulties in calculating more complex structure patterns from integrated measurements. [13]

The principle with the rotating needle was abandoned due to the system is quite complex to produce, and that available commercial tends to have a minimum resolution of 8x8 μm. [4]

The choice of measuring principle fell on a CCD based solution, as this is the technique identified with the best available resolution without introducing lenses. A CCD solution is preferred over a CMOS as the spectral response from most CMOS chips at the laser wavelength is quite low or non-existing as shown in fig 1.

![Spectral Sensitivity](image)

**Fig. 1.** Spectral response for CCD, CMSO and human eye with laser wavelength (1070 nm) and wavelength for measured camera response (633 nm). [6]

The final choice of product fell on Laser Beam Analyser SP620 U from Ophir-Spiricon, as it where the camera identified in the search with best spatial resolution and highest dynamic range. The camera does not fulfil the requirements for spatial resolution given. This demand is disregarded, as choice has been made on the best available technique and camera which were identified. Also it is considered to be important not to introduce lenses in the beam path to enlarge the beam. This has been important for avoiding further aberrations in the beam. The 1,7 μm resolution demand originates from the necessary resolution with a 200 mm focus lens. Using a 500 mm lens the demand is met.

The SP620 U camera comes with beam analysis software, BeamGage, with functions to correct for average background noise, diameter calculations etc.
Table 3. Data for the SP620 camera [5]

<p>| | |</p>
<table>
<thead>
<tr>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Spectral Response</strong></td>
<td>190 - 1100nm</td>
</tr>
<tr>
<td><strong>Maximum beam size</strong></td>
<td>7.1mm x 5.4mm</td>
</tr>
<tr>
<td><strong>Pixel spacing</strong></td>
<td>4.40μm x 4.40μm</td>
</tr>
<tr>
<td><strong>Number of effective pixel</strong></td>
<td>1600 x 1200</td>
</tr>
<tr>
<td><strong>Frame rate</strong></td>
<td>7.5 fps @ full resolution</td>
</tr>
<tr>
<td><strong>Camera dynamic range</strong></td>
<td>62 dB</td>
</tr>
<tr>
<td><strong>Saturation intensity</strong></td>
<td>2.2 μW/cm²</td>
</tr>
<tr>
<td><strong>Lowest measurable signal</strong></td>
<td>2.5 nW/cm²</td>
</tr>
<tr>
<td><strong>Damage threshold</strong></td>
<td>0.15 W/cm²</td>
</tr>
<tr>
<td><strong>Image depth</strong></td>
<td>12 bit</td>
</tr>
</tbody>
</table>

1 @632.8nm wavelength

The chip in the camera is a silicon based CCD chip. The response for a CCD chip is shown in fig. 3. The two red lines indicate the laser wavelength (1070 nm) and the wavelength, for which Ophir-Spiricon has given the saturation and lowest measurable signal (633nm). From the curve it can be seen, that the expected signal for a laser with 1070 nm wavelength is about half of the signal at 633 nm. This means that the lowest measurable signal and saturation is about twice as high as given in table 3.

Transferring the 12 bit A/D conversion gives 72 dB. The spec is however 62 dB. This is due to some natural noise on the chip and in the digitizer. To keep the signal to noise ratio during measurements high, the noise must be kept low. To do this the gain should be kept low, and no light from surroundings must enter the camera.

The beam needs to be attenuated before entering the camera. To do this a series of beamsplitters and ND-filters are introduced in the beam path. In fig. 2 the unwrapped beam path is shown in one plane.

![Fig. 2. Sketch of unwrapped beam path. The 4 blue wedges are wedge shaped beamsplitters.](image-url)
The beam splitters are wedge shaped, and the rear side are AR-coated. The wedge shape further result in that the transmissive part of the beam is bent a little as shown on fig. 2 and 3. The beam dump is placed far away from the beam splitters and camera to allow the beam to expand again before being absorbed, so the intensity is reduced to below the damage threshold. As beams with focal length up to 780 mm are to be analysed with the system a large distance from camera to beam dump is needed.

![Fig. 3. CAD-model of the constructed setup shown with no cover plates.](image)

To attenuate the maximum intensity from the laser beam to “saturation intensity”, a set of beam splitters and ND-filters is applied. For minimising problems with alignment two beamcubes from Ophir Spiricon is used. These are manufactured to align the beamsplitters wedges with only little work and no need for adjustment off each splitter and align with the camera entrance. This gives four beamsplitters before the beam passes the ND-filters.

At the setup at Aalborg University two different focussing heads is applied, both with a collimating focal length of 200 mm and each with two different focussing modules ranging from 200 to 780 mm focal length. This gives focus spot sizes from 34 to 134 μm when applying the focussing heads without DOEs, and center intensities as given in Table 4, when applying the full 3 kW. Since the full power is not allowed to enter the chip, attenuation is needed. This is given as ND-values and is calculated from eq. 1

\[
ND = -\log_{10}(T)
\]

\[eq. 1\]

\(T\) is the percentage of the beam, which continues towards the chip.

**Table 4. Data for beam to camera system and required attenuation.**

<table>
<thead>
<tr>
<th>Focal length (mm)</th>
<th>(D_{\text{min}}) (μm)</th>
<th>(Z_R) (mm)</th>
<th>(I, \text{center}) (MW/cm²)</th>
<th>Round beam 3 kW</th>
<th>Round beam 30 W</th>
<th>Beam pattern 30 W</th>
</tr>
</thead>
<tbody>
<tr>
<td>200</td>
<td>34</td>
<td>0,71</td>
<td>6,6</td>
<td>15,3</td>
<td>13,3</td>
<td>11,5</td>
</tr>
<tr>
<td>300</td>
<td>51</td>
<td>1,6</td>
<td>2,9</td>
<td>15,0</td>
<td>13,0</td>
<td>11,2</td>
</tr>
<tr>
<td>470</td>
<td>80</td>
<td>3,9</td>
<td>1,2</td>
<td>14,6</td>
<td>12,6</td>
<td>10,8</td>
</tr>
<tr>
<td>780</td>
<td>133</td>
<td>10,8</td>
<td>0,43</td>
<td>14,2</td>
<td>12,2</td>
<td>10,4</td>
</tr>
</tbody>
</table>

\(^1\)The center intensity for a Gaussian beam is two times as high as the average intensity
In table 4 is also given the minimum spot diameter in focus when the focusing heads are applied without DOEs, $D_{\text{min}}$ and the Rayleigh length, $Z_R$. The different focal lengths give a need for different ND-values. The intensity in focus also depends if a pattern is generated or not, and at which power level the laser is set. Only the extremes are shown in table 4. These gives that the attenuation should be changeable from ND 10.4 to ND 15.3, giving a ratio of $97.344 = 99.7$ dB. This dynamic is built into the system by fixed beamsplitters and changeable ND-filters. Values are given in table 5.

The minimum diameter and Rayleigh length are calculated from eq. 2 and 3
\[
D_{\text{min}} = \frac{4\lambda M^2 f}{\pi D_0} \quad \text{eq. 2}
\]
\[
Z_R = \frac{D_{\text{min}}^2 \pi}{4\lambda M^2} \quad \text{eq. 3}
\]

$f$ is the focal length, $M^2$ is the beam quality factor, $D_0$ is the collimated beam diameter on the focusing lens and $\lambda$ is the wavelength.

<table>
<thead>
<tr>
<th>ND-value</th>
<th>Options</th>
</tr>
</thead>
<tbody>
<tr>
<td>Splitter 1</td>
<td>2 Fixed</td>
</tr>
<tr>
<td>Splitter 2</td>
<td>2 Fixed</td>
</tr>
<tr>
<td>Splitter 3</td>
<td>2 Fixed</td>
</tr>
<tr>
<td>Splitter 4</td>
<td>2 Fixed</td>
</tr>
<tr>
<td>Variable ND-filter</td>
<td>0 to 3.9, 0 – 0.4 – 0.8 – 1.0 – 2.0 – 3.0 – 3.9</td>
</tr>
<tr>
<td>Variable ND-filter</td>
<td>0 to 3.9, 0 – 0.4 – 0.8 – 1.0 – 2.0 – 3.0 – 3.9</td>
</tr>
<tr>
<td><strong>Sum</strong></td>
<td><strong>8 to 15.8</strong></td>
</tr>
</tbody>
</table>

### 4 Tests

To test the system a HighYag BIMO focusing head is applied, and the beam is directed through the attenuation system to the camera where images are recorded and stored. Two types of tests were performed: Beam caustics at different power levels and evaluation of patterns created by DOE’s.

#### 4.1 Measurements of beam patterns

When producing beam patterns with DOE’s there will often be some scattered light. Results and measurements from one DOE are shown in fig. 4. Image is only evaluated within a certain area of the CCD. This area is set so that areas which contain scattered light and beam pattern is included, but areas which cannot be distinguished from the background noise is excluded. Further a process zone is defined. This is light, which is not in the pattern, but still is in the area which will be melted or removed during cutting or welding. This is chosen because this light will still be absorbed in the process zone.
As seen on figure 4 and the corresponding table the evaluation of patterns produced by DOE’s can be performed. The results do also show some difference of design compared to measurements. This gives feedback to the manufacturing and designing process.

### 4.2 Measurements of beam caustics

Two sets of measurements are shown in fig. 4. The corresponding caustics calculations are shown in fig. 5.
Fig. 6. Caustic for fig. 4. Diameter measurements according to ISO 13694. [8]

From fig 6 some difference between focus position and focus diameter are observed.

4.3 Comparisons

The diameter measurements in fig 6 have been fitted using eq. 4 [7] by least square method. This gives information on the focus position, focus diameter and the beam quality factor.

To evaluate the goodness of the coefficient of determination – $R^2$, is calculated

$$D(z) = D_{min} \sqrt{1 + \left(\frac{4M^2\lambda^2}{\pi D_{min}^2} (\frac{z_{focus}-z}{z_{focus}^2})^2\right)^2}$$

Eq. 4.

Least square method is used instead of ISO 13694, despite ISO 13694 recommends not to give equal weight to central region and wings of caustic. [8] The purpose is here just to compare the two measurements with 780 mm focus lens, and evaluate the measurement system and stability.

In connection to the acceptance test of the laser source a set of beam measurements where performed with a different HighYag BIMO focussing head, using a Primes FocusSpotMonitor at different power levels. These measurements are shown on fig. 6.
In table 6 results from the fit of eq. 4 to the measurements in fig. 6, and caustic with f300 (Optoscand focusing system) combined with the acceptance test measurements done with Primes FocusSpotMonitor are collected.

**Table 6. Results from the two caustic measurements**

<table>
<thead>
<tr>
<th>Measurement system</th>
<th>SP620</th>
<th>SP620</th>
<th>SP620</th>
<th>Primes</th>
<th>Primes</th>
</tr>
</thead>
<tbody>
<tr>
<td>Focussing head magnification</td>
<td>3.9</td>
<td>3.9</td>
<td>1.5</td>
<td>4.2</td>
<td>4.2</td>
</tr>
<tr>
<td>Laser power (W)</td>
<td>60</td>
<td>1290</td>
<td>550</td>
<td>1000</td>
<td>3000</td>
</tr>
<tr>
<td>Beam quality factor, M₂</td>
<td>1.32</td>
<td>1.18</td>
<td>1.35</td>
<td>1.3</td>
<td>1.28</td>
</tr>
<tr>
<td>Dfocus (μm)</td>
<td>124</td>
<td>137</td>
<td>53</td>
<td>138</td>
<td>150</td>
</tr>
<tr>
<td>Relative focus difference (mm)</td>
<td>+5.5</td>
<td>-</td>
<td>-</td>
<td>+0.8</td>
<td></td>
</tr>
<tr>
<td>R²</td>
<td>0.97</td>
<td>0.97</td>
<td>0.99</td>
<td>-</td>
<td>-</td>
</tr>
</tbody>
</table>

As can be seen there is two very different focus positions and focus diameters for the two measurements done with the 780 mm lens (magnification 3.9). This is most likely due to some thermal drift of either the optical head or the measurement system. It has not been possible to distinguish, whether this is coming from the optics in the camera or from the optics in the head itself.

Measurements done with the Primes FocusSpotMonitor and a different optical head do also give a little difference in the focus diameter and focus position. The Primes FocusSpotMonitor cannot have any thermal drift in the measurement system due to the way it is constructed, so here the difference must originate from either the laser or the processing head. The processing head is unfortunately not the same, so no conclusion can be made from this.

The measurement of M₂ gives values which all are close to each other, and close to the value promised by laser manufacturer. The M₂ = 1.18 differs slightly, but is within what could be expected.

The acceptance test where done with a different laser head, with different focal and collimation lenses. The magnification is however almost the same. Therefore the focus diameter should also be close to each other, which table 6 also show.
The focus diameter, M2 and focus position should be independent of power. This does however not seem to be true for the measurements. Further there is a change in the focus position from low to high power. This might be due to thermal drift in the laser head, or the measuring system. However the Primes FocusSpotMonitor cannot have any thermal drift due to way it performs the measurement with no optics.

6 Future work

To examine how much of the thermal drift that originates from the leaser head and how much coming from the measurement system there is planned a set of measurements. In these measurements the beam is left on with high power for a long period away from the camera system and then quickly moved onto the camera. This shall be compared to experiments, where the beam is on for a long period directly on the camera. In this way it should be possible to isolate long term thermal drift from the laser head and the measurement system.

Also the calculations of beam parameters by curve fitting are planned to be done with a closer link to ISO 13694. This requires an update of the algorithms used.

7 Conclusion

The ability to measure scattered light and evaluate DOE’s has been demonstrated.

The spatial resolution of the system are sufficient for most cases, only evaluation of focused beams with magnification of less than 2.5 is a problem according to the ISO-standard. Smallest magnification available is 1.0. In this case the beam will only fill 8 pixel in any direction. Measurements can still be performed so operator can verify if the beam looks reasonable, just not according to standard.

The chosen CCD based camera from Ophir Spiricon was the best camera identified on the market with a high dynamic range without introducing possible aberrations and alignment problems from lenses.

Even though the demand for 20 pixels in any direction from ISO 13694 is not met for magnifications below 2.5 the system can be used to quick information on the produced pattern. If measurements in accordance to the standard are needed, then a switch to longer focal length or shorter collimation length is needed.

Further work is necessary to evaluate if the system suffers from thermal drift. It has so far not been possible to decide whether this drift comes from the measurement system or the laser head.

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   “Lasers and laser-related equipment — Test methods for laser beam widths, divergence angles and beam propagation ratios”
   “Optics and photonics – Lasers and laser-related equipment – Test methods for laser beam power, energy and temporal characteristics.”
PERMANOVA FOCUS POSITION SYSTEM

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Abstract

Permanova now releases a brand new version of the patented Focus Position System, FPS. Some of the new features are; completely new software and user interface, water cooled beam dump for higher power handling and it is based on modern GigE camera technology. Despite all technical news, the retail price of the system has been reduced!

The Permanova FPS measures the X, Y and Z-coordinates of the focus position of the beam. In the case of a cutting tool you will also get X, Y and Z-coordinates of the nozzle. This enables a fully automatic check of the beam to nozzle alignment, focus position and TCP during production. It also serves as a great aid during maintenance. The FPS may be used to center the beam in the nozzle, measure the beam and nozzle position, adjust robot TCP and get back to work quickly. It also measures the angles of the beam.

The FPS connects to a PLC or robot via any of the major industrial bus systems, like Profibus, Interbus etc. It can also be connected to your factory network enabling remote monitoring via log files and saved snapshots of beam to nozzle centering.

Keywords: focus position system, TCP, FPS, XYZ-coordinates, laser, beam angle, remote monitoring, cutting nozzle centering, log files, snapshots, GigE camera, beam diagnostics.

1 Introduction

During laser processing it is essential to monitor the status of your process tool over time and thereby reducing down time and improve both quality and productivity. This is valid for both cutting and welding processes. However, in the case of a cutting process it is even more important due to the extra complexity introduced by the two important parameters; beam to nozzle centering and beam focus to nozzle distance (hereafter named focus distance). Traditionally checking the nozzle centering and beam to nozzle distance has been done through trial and error until a good result was achieved. To simplify this procedure drastically Permanova some years ago released the FPS, Focus Position System. Now the second generation reaches the market.

The patented Permanova FPS measures the focus position of a laser beam in the X-, Y-, and Z-coordinates. It can also perform automatic measurements of the cutting nozzle position in X, Y and Z and thereby making it possible to fully automatically verify the beam
to nozzle centering, that is so vital to a good cutting result. It also serves as a nozzle-centering monitor for cutting nozzles during setup. Using the live view mode you can adjust the centering using the high power beam.

In addition to the positions of both beam and nozzle the Permanova FPS measures the angle of the laser beam relative to the sensor. This makes it possible to also update the TCP of your robot/manipulator.

Use the FPS to center the beam, measure the beam position, adjust your robot TCP and get back to work. Make a stop at the FPS every now and then, to verify that the beam and the tool stay in shape.

Figure 1: Focus distance illustration.

2 Overview of the System

The main component in the system is the sensor, see Figure 2. It connects to a PC which handles the motor control and image processing. The system can be run from a normal laptop or optionally from a supplied fanless PC.

Figure 2: The sensor unit with optional mounting stand, PC, keyboard, screen, and documentation.
A robot/manipulator positions the laser processing tool in front of the sensor in order to measure the focus position, see Figure 3.

![Image](image1.png)

*Figure 3: The laser tool is placed in front of the sensor unit in order to measure the focus position. The laser beam goes into the sensor unit where it is scanned. The focus position is found through image analysis.*

### 2.1 Measurement Principle

The FPS contains a movable camera connected to control software developed by Permanova, PermaFPS. The software contains advanced image processing and state of the art curve fitting techniques for robust and repetitive measurements. The camera system is sensitive to both visible light as well as invisible laser light. This makes it possible to observe objects (like cutting nozzles) illuminated with visible light, in our case strong LED with wavelength 504 nm. It is also possible to study laser light directly instead of the light's secondary effects, as with some other sensors.

Through image analysis it is straightforward to detect the position of edges. Not only the X and Y positions can be detected with the help of image analysis; it is also possible to calculate image sharpness. By moving the camera and performing sharpness analysis along the nozzle edges in different Z positions the best position can be found. This way it is possible to find the coordinates for a hole (like for a cutting nozzle) with very high accuracy, see Figure 4.

![Image](image2.png)

*Figure 4: The sharpness of the image is highest at only one z-position.*

The focus of the beam is found in a similar way. In this case the beam is sent straight into the camera. Because of the short depth of focus of the camera optics, one can say that the beam is “sliced” by the camera perpendicular to its direction of travel. Therefore one can find the size and X and Y positions of the beam for a specific Z position. When moving the camera along the Z-axis, measurements are performed. By scanning the beam in many positions it is possible to find the smallest and sharpest Z-position of the beam (see Figure 5). This position corresponds to the focus of the beam.
In the aforementioned scanning method of the beam, the image analysis software also finds the X and Y values of the beam. If the beam does not come straight in to the camera, but rather at a slight angle, the X and Y values will change during the scan. Using three dimensional curve fitting techniques it is possible to determine the angle of incidence of the beam relative to the sensor.

For clarity the measurement principles were described separately but please note that in reality they are most often done simultaneously!

As shown in the pictures above, it is clear that the FPS can also be used to get a clear view of the beam and nozzle relation. The X and Y measurements can be used to center the beam in the nozzle.

2.2 FPS Coordinate System

Figure 7: The sensor unit with its origin and axes. The z-axis coincides with the optical axis.
All measurements are made relative to the sensor unit. The origin of the coordinate system is placed approximately 30 mm outside the unit, see Figure 7. If absolute measurements are required, it is necessary to calibrate the FPS to another known coordinate system. The well-defined reference points on the front of the sensor can then be used to locate the sensor coordinate system using a robot for example.

In most cases, taking only relative measurements is sufficient because it is normally only necessary to detect changes in robot or laser tools in order to prevent downtime of a laser system.

2.3 Sensor Unit

Figure 8 shows the sensor unit with the green LEDs that are used to light up the nozzle so that it can be located by the camera.

*Figure 8: Picture of actual sensor unit with the green LEDs lit.*
The sensor unit consists of a camera with optics and a water-cooled beam dump mounted on a linear drive with a servomotor. The optics and camera is a fixed package to simplify calibration, see Figure 10. During the measurement of the z positions the camera and optics are moved in the z-direction. Multiple images are captured during the movement. The position of the drive is synchronized with the images. From this information the PC can determine the z position of the nozzle surface and the beam focus.

2.4 Automatic System Calibration

The Permanova FPS can perform an automatic self-calibration by mounting a supplied calibration cup on the front face of the sensor.

The calibration cup is mounted on the FPS sensor unit. The cup has 3 pins in the image plane and they define the nominal position, see Figure 11. The variations in camera position etc are then taken into account and saved as an offset in the PermaFPS-software. Thereby making it possible to perform an absolute calibration of your cutting/welding tool TCP!
3 The PermaFPS Software

Permanova has developed brand new software for the FPS.

A complete measurement cycle takes approximately 3-5 seconds, depending on how long the measurement is in Z-direction. The measurement length is optimized based on your beam to nozzle distance and depth of focus of the laser process tool.

After each measurement the software displays two images (in case of simultaneous beam and nozzle measurement). The images show the positions were the beam and nozzle was in focus, hereafter referred to as BestBeam and BestNozzle images. Also a logfile is generated which continuously logs X, Y and Z of both beam and nozzle along with the beam angles A and B.

3.1 Remote Monitoring

By connecting the FPS controller to a factory network it is possible to monitor the process tool status remotely. The BestBeam and BestNozzle images along with the log files always displays the latest status since the system continuously replaces them with updated versions.

3.2 One Controller, Several Sensors

Each controller can handle up to three sensors, thereby reducing both cost and complexity of the installation.

3.3 Robot/Manipulator Connection

The FPS can be used as a standalone calibration and setup aid. But it is usually connected to a robot to take full advantage of the fully automatic measurements it offers. The connection can
be realized through any standard industrial bus such as Profibus, Profinet, Interbus, Devicenet, etc. Once the FPS is connected it can be completely controlled by the robot, all measurements are triggered from the robot and all results are transferred to the robot for evaluation and possible TCP update.

3.4 Measurement Accuracy and Curve Fitting

To improve the accuracy and repeatability of the FPS an advanced curve fitting technique is utilized. It is based on the Downhill Simplex method by Nelder and Mead [1] but in a more robust form using Simulated Annealing, originally described by S. Kirkpatrick, D.C. Gelatt and M.P. Vecchi [2]. Figure 13 below shows measured graph along with the fitted curve (bold). In Figure 14 the difference in repeatability is shown with and without the curve fitting. The standard deviation in Z-position has gone from about 0.14 mm down to 0.002 mm under ideal conditions.

![Measurement graphs](image)

**Figure 13:** Beam graph with a fitted Gaussian curve in bold.

![Repeatability graphs](image)

**Figure 14:** Repeatability of a nozzle measurement, with (l.h.) and without (r.h.) curve fitting.

3.4.1 Beam Angle Measurements

Since the FPS can measure X and Y positions of the beam in multiple planes along the beam it is possible to calculate also the direction of the laser beam in space. This is a more straight
forward linear curve fitting problem which is easily solved using three dimensional least squares fitting. From this, the two angles A and B are calculated. They represent the angles around the X- and Y-axis. Figure 15 shows a 3D-plot of the laser beam in space. Since it is difficult to see the actual curve fit here Figure 16 shows the two dimensional projections of the curves.

Figure 15: Three dimensional plot of the laser beam angle.

Figure 16: Two dimensional projections of the laser beam angles.

### 3.5 Power Handling Capabilities

The FPS is designed for laser power up to 600W during 30s or 200W continuously. It can however handle higher power during short periods of time should the need arise. It contains a water cooled beam dump which is monitored by a temperature interlock in combination with a special interlock card which breaks the interlock when heated. The interlock signals are connected to the safety inputs of the laser to stop the laser in case of danger to the equipment. Figure 17 shows two temperature measurements inside the FPS during 120 seconds of 200W and 600W continuous laser power. Only about 7 degrees of temperature rise is recorded for 200W which then reaches steady state. For 600W a rise in about 20 degrees is recorded before steady state is reached. The interlock sensor will switch off the laser at when the beam dump reaches 70 degrees.
4 Conclusions

The Permanova Focus Position System can help laser users monitor the status of their process tools. Preventing down time and reducing time during tool exchanges and TCP updates. The system is applicable to both cutting and welding processes, although main area of uses comes from the cutting applications. The FPS is now available at a low price and should be mandatory when planning a laser cutting station.

5 References


Figure 17: Temperature measurements of the FPS during 120 seconds at 200 and 600W.
Customized laser optics and processes for inner tube cladding.

Theodor Fleitmann
Torsten Bady
About NUTECH

Managing Director
Theodor Fleitmann
61 Employees

3 Departments

Lasercenter
Dipl.-Ing. Axel Rach
Lasermaterial Processing from Development to Series-Production; Certified by ISO 9001:2000

Center Analysis & Testing
Dipl.-Ing. Peter Lippert
Analysis and Testing of Materials and Components; Accredited by ISO 17025

Lasersystemtechnologie
Dipl.-Ing. Torsten Bady
Development, Construction and Delivery of customized Laserbeam - Tools and of Facilities

NOLAMP 14, Gothenburg, August 26-28, 2013
Some facts about our Laser Job Shop Center

- We’re welding > 3.000 km/year seams at brake-shoes
- Since 1997 we welded > 120 Mio belt-retractor-pinions
- We have > 15.000 km of laserwelding experience
- We serve each year > 350 customers
- We’re running 15 Lasers (cw/puls/Q) from 40 W to 6 kW on 19 machines (Universal- or single purpose)
- We realized in 25 years about 25 laser projects
About NUTECH

Center of Analysis and Testing
accredited by DIN EN ISO 17025

- Material Testing
- Material Analysis
- Calibration Services

About NUTECH

Laser System Technology

- Construction of Laserbeam - Tools
- Development of Laser - Processes
- Supplying of Facilities
Material
Inconel
or 1.4301
Diameter
15mm
Laserpower
max 4 kW
Nd:YAG-Laser

Laserwelding 3 kW Nd:YAG-Laser. ID 25 mm tube
Optics for laser processing

Crackoptic for Piston Rods:
- Grey cast iron
- Diameter of rod eye: min. 40 mm
  - Pulsed Fiber Laser
  - or Nd:YAG Laser (direct beam or fiber connected)
- Laserpower: 50W / 50kHz

Welding of Cupped Casings
- Heat-conductive welding
- 5-Axis Robot
  - < 6 kW HPD Laser
  - Fiber coupled
  - Increased process time to factor 3
Optics for laserprocessing

Laserhardening
Id 8 -10 mm Ø

Laserleistung
2 kW

Ringfocus

Laserhardening
Id 120mm Ø
Tube length
2 m

Laserpower
2 kW

Beamshape:
Square 4 mm
60 tracks
Hardness
penetration depth
1 mm
600 HV
35NiCrMoV

Optics for laserprocessing
Optics for laserprocessing

Rotating laser-optic for hardening and alloying of motorblocks and cylinderliners

ND YAG Laser 4 kW
Tool kit system for Laser optics which can be pieced together according to customer requirements and are thus perfectly qualified to ensure a safe manufacturing process inside tubes.
Optics for inner tube cladding

- Tube diameters starting at 50 mm
- Faserlaser, Nd:YAG Laser, Dioden Laser
- Working depth up to 2,000 mm
- Typ. process power 2 – 3 kW

Cladding in narrow tubes: Start up

- Different Laser types up to 6 kW
- Inner Diameters 70 mm
- Working depths up to 1000 mm
- Rotating tubes
Specifications for a customized optic design:

- Type of laser and the BPP
- Your process power for cladding
- Beam spot diameter on the surface
- Minimum inside diameter of the tube
- Max. Working depth inside tubes
- Fiber diameter you wish to use
- Beamshape

mm mrad
kW
mm
mm
µm
round / square
Cladding process
Cladding process
Cladding process

- Basic Parameters for cladding:
  - Laser
    - Beamspot area: appr. 20 mm²
      - round: (ø 2mm ... ø 5mm)
      - square: (a:...3 mm x b:...7 mm)
    - Intensity on the spot: (100 ...200 W / mm²)
    - Process velocity: (0,7 – 1,5 m/min)
    - Process power: up to 4 kW
  - Powder
    - Specification: (div.)
    - Powder focus: (< laser focus )
    - Flow / volume: (30 ... ? g / min)

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<td>2,4</td>
<td>24</td>
<td>2,18</td>
<td>2,18</td>
<td>2,18</td>
</tr>
</tbody>
</table>
Cladding process

Quality specifications:
- Trackheight
- Surface
- Dilution
- Cracks
- Pores
- Bondage
- Clad elements

Nothing is really perfect
Nothing is really perfect
Cladding process

.................sometimes it is!!
Industrial laseroptics and processes in pipes

- pipe diameter above 2 inch
- Working depth actually max 2,000 mm
  - (4,000 mm from both sides)
- Cladding and hardening
- Stand off time 8 hrs and more
- Process similar to od cladding
- Optics for pipes 10 m and longer still to come

thank you for your attention
APPLICATION OF HIGH DYNAMIC PROCESSING HEADS FOR LASER CUTTING

F. KLENKE, J. HAUPTMANN
Fraunhofer Institute for Material and Beam Technology IWS

Abstract

This paper presents solutions for high dynamic and accurate 2D laser cutting. These solutions are based on a laser processing head with an integrated high dynamic axes system for the positioning of the laser beam. Additional to the application as stand-alone system the processing head can be applied superimposed to a conventional main axes system of a laser cutting machine. In this case the trajectory for the relative movement between the laser beam and the workpiece has to be splitted into one trajectory for the main axes system of the laser machine and one trajectory for the high dynamic axes system of the processing head.

Keywords: positioning control, optimal control, superpositioned axes system

1 Introduction

State-of-the-art laser sources have considerable lifted the processing speed at the laser cutting. Especially solid state lasers, as fiber or disc lasers, realise in comparison to CO₂ lasers significant higher cutting speeds at the same laser output power. This benefit can effect either the reduction of the laser power or the increase of the productivity of the laser machine. As shown in Fig. 1 the typical cutting speeds depend e.g. on the material thickness of the workpiece and the laser output power. Already at low laser output power the typical cutting speeds rise significantly towards smaller material thickness.

With regard to conventional cutting machines these speed values often cannot be exploited. Due to the limit values for acceleration and jerk, at the processing of complex contours with many direction changes over the whole path length, the average speed and thus the processing time are strongly below the technological limits. Fig. 2 illustrates the influence of the dynamic limit values maximal jerk and maximal acceleration on the average movement axis speed in case of point to point (PTP) movements. In case of a PTP movement with a travelling distance of 5 mm in the range of jerk values of nearly up to 1000 m/s³ maximal acceleration limit values greater than 10 m/s² have no influence on the average processing speed. During the positioning process the jerk value switches between its positive and negative limit values whereas the maximal acceleration limit value isn’t reached. Only by further augmentation of the jerk limit value the average processing speed can be increased. Connected to this, the augmentation of the acceleration limit value would raise the average processing speed.
Towards larger travelling distances, such as 50 mm in case of Fig. 2 right, the augmentation of the maximal acceleration limit value already has effect on the average processing speed at lower jerk limit values.

2 **High Dynamic FormCutter HDFC<sub>6060</sub>**

In order to convert the cutting capacity of modern laser sources into the contour cutting, at the Fraunhofer IWS an innovative laser cutting machine called High Dynamic FormCutter HDFC<sub>6060</sub> is developed. The HDFC<sub>6060</sub> is designed for the high dynamic and accurate cutting in an x/y working field of 60 x 60 mm<sup>2</sup>. The specifications of the HDFC<sub>6060</sub> are listed in Table 1. The high dynamics are achieved by an innovative x/y parallel kinematics, the reduction of moved masses and the consequent use of linear direct motors. In terms of accuracy the parallel kinematics lead to the same moved masses and dynamic behaviours in x and y direction. The integrated high dynamic z-axis with a stroke of 20 mm in combination with a capacitive distance control guarantees an accurate working distance. An additional z-axis for movements of the whole cutting head in z-direction can be omitted.

The HDFC<sub>6060</sub> can be installed as fully-fledged cutting system for the high-productive mass production of complex components. Thereby the HDFC<sub>6060</sub> can be installed fix on a framework and the workpieces are sequentially put through under the processing head.
A method to overcome the boundary of the limited working field and to further provide the high dynamics of a processing head like the HDFC\textsubscript{6060}, the processing head can be coupled with a conventional main axes system of a laser cutting machine. In this way the acceleration capacity of the high dynamic axes system is transferred to the whole working field of the main axes system.

At the simultaneous operation of a high dynamic axes system like the HDFC\textsubscript{6060} and the conventional main axes system of the laser cutting machine the trajectory of the relative movement between the laser beam and the work piece has to be splitted into position trajectories for both axes systems. Selected benefits of this solution are:

- Reduction of the processing time depending on the contour, the working field of the high dynamic axes system and the dynamic limits of both axes systems.
- High accuracy
- Due to the reduced processing time and the realizable smoother movement of the main axes system, the total energy consumption of the laser machine can be significantly reduced.

The experimental setup for coupled axes systems at the Fraunhofer IWS is shown in Fig. 4. The HDFC\textsubscript{6060} is mounted on a fixed bridge. The x/y cross table below the bridge moves the workpiece.

### Table 1. Characteristics of the High Dynamic FormCutter HDFC\textsubscript{6060}

<table>
<thead>
<tr>
<th>Characteristic</th>
<th>Value</th>
</tr>
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<tbody>
<tr>
<td>Working field:</td>
<td>60 x 60 mm(^2)</td>
</tr>
<tr>
<td>Max. stroke of the z-axis:</td>
<td>20 mm</td>
</tr>
<tr>
<td>Max. axis speed:</td>
<td>1 ms(^{-1})</td>
</tr>
<tr>
<td>Max. axis acceleration:</td>
<td>30 ms(^{-2})</td>
</tr>
<tr>
<td>Positioning precision:</td>
<td>± 10 (\mu)m</td>
</tr>
<tr>
<td>Outer dimensions:</td>
<td>280 x 250 x 230 mm(^3)</td>
</tr>
<tr>
<td>Solid-state laser:</td>
<td>&lt; 5 kW</td>
</tr>
<tr>
<td>CO(_2) laser:</td>
<td>&lt; 2.5 kW (on request)</td>
</tr>
<tr>
<td>Cutting gas:</td>
<td>N(_2), O(_2) &lt; 15 bar</td>
</tr>
<tr>
<td>Distance sensors:</td>
<td>integrated</td>
</tr>
<tr>
<td>Weight of the entire module:</td>
<td>15 kg</td>
</tr>
</tbody>
</table>
3.1 Time optimal positioning control of a coupled axes systems

In this section the time optimal positioning control of a coupled axes system is presented. The trajectories for the x and y axes of the main and the high dynamic axes system are calculated by the use of linear programming techniques with respect to the dynamic limits of the axes systems. The dynamic limit values of the axes systems are listed in Table 2. Fig. 5 shows the analyzed contour and the optimized path of the center of the high dynamic axes system in relation to the corresponding second contour section. The arrows illustrate the distances from the start and end points P1 and P2 of the second contour section which are compensated by the high dynamic axes.

Table 2. Dynamic limit values of coupled axes systems

<table>
<thead>
<tr>
<th></th>
<th>Main axes system</th>
<th>High dynamic axes system</th>
</tr>
</thead>
<tbody>
<tr>
<td>Max. jerk:</td>
<td>300 ms(^{-3})</td>
<td>1500 ms(^{-3})</td>
</tr>
<tr>
<td>Max. acceleration:</td>
<td>10 ms(^{-2})</td>
<td>30 ms(^{-2})</td>
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</table>

Fig. 5. Test contour – time optimal positioning control
Before the beginning of the breaking phase along the first contour section the relative movement between the TCP and the workpiece is only realized by the main axes system. The path velocity, as shown in Fig. 8, is at its maximum value.

The breaking process is introduced by a short movement of the high dynamic axes system in the direction of the main axes movement. This movement is also performed by the main axes system additional to its ongoing movement at maximal path velocity. Afterwards the high dynamic axes system accelerates in the opposite direction of the main axes system movement in exactly the same way as the main axes system decelerates. During this period the velocity of the TCP along the path is still at its maximum value. Accordingly, the change of the movement quantities of both axes systems is limited by the dynamic limit values of the main axes system.

In the following part of the breaking process the velocity of the relative movement between TCP and workpiece is reduced. This phase begins outgoing from the time optimal start values for acceleration and velocity with the abrupt change of the jerk value of the high dynamic axes system upon its negative limit value, compare to Fig. 7-9. The dynamic limit values of the relative movement between the TCP and the workpiece during this part of the breaking phase are a superposition of the dynamic limit values of both axes systems. Thus the effective maximum values of deceleration and jerk of the TCP along the path are -40 m/s² and ±1800 m/s³. The time optimal breaking process is finished (point P1) when the axis systems are synchronized. In this case the values of velocity and acceleration of the axes systems are equal.
By reaching point P1 the movement of the TCP along the contour section 2 in y direction begins. At the starting point the superposed axis is deflected by half of the total travelling distance. The time optimal start and end values of the motion quantities acceleration and velocity are shown in Fig. 11, 12. The movement of section 2 can be enlarged by synchronized movements of the main and superposed axes systems in this way that the movement of the superposed axis starts and ends in the middle of its working range, compare to Fig. 10. Consequently, at the end of this movement sequence the total travelling distance of the main axes system equates the length of the second path section. The maximal path velocity of 1,25 m/s could be achieved due to the superposition of the dynamic limit values of both axis systems.

3.1 Splitting Algorithm REDcut

At the Fraunhofer IWS the software REDcut for the splitting of the contour trajectory into the trajectories for the main and the high dynamic axes systems was developed.

The basic algorithm of the REDcut software is known in literature [1], [2]. It is based on the concept of the smooth moon landing which was adapted to the necessities of the laser cutting process. First of all a tool for the dimensioning of dynamic systems was created. The dependencies i.e. of acceleration, jerk and working field can be investigated depending on the part geometry. Beside the offline version of the software REDcut for theoretical calculations
and dimensioning an online version exists. REDcut online is used for the realization of the superpositioning of axes at the IWS laboratory.

The REDcut algorithm is located in the machine control as shown in Fig. 14. Outgoing from the NC-Code the Interpolator calculates the motion trajectories for a virtual axes system which dynamic properties represent the dynamic behavior of the coupled axes system. Based on these trajectories the block REDcut algorithm calculates the trajectories for the main axes system. For the realization of a look ahead along the path the trajectories for the virtual axes system are delayed. These delayed trajectories are the command trajectories for the superpositioned movement of the coupled axes system. The principle behavior of the REDcut algorithm is demonstrated in case of a square contour in Fig. 15, 16.

**Fig. 14. Structure of the REDcut algorithm**

**Fig. 15. Contour and path of the main axes movement**

**Fig. 16. Velocities of main and high dynamic axes, path velocity**

### 4 Conclusion

The dynamic limit values of a cutting machine determine the average processing speed and thus the processing time. Especially at complex contours with many direction changes over the whole path length in terms of time optimality high jerk values are aimed. These high jerk values strongly affect the structural design of the cutting machine. In this paper a laser processing head with an integrated x,y,z axes system is presented which is designed for the high dynamic and accurate cutting of complex contours in a limited working field of 60 x 60 mm². For the enlargement of the working field the high dynamic processing head can be coupled with a conventional axes system of a laser machine. The dynamic values of the laser machine can thus be highly increased. As presented in case of the time optimal simultaneous positioning of the high dynamic axes system and the conventional main axes system of the laser machine, the maximal dynamic values are a superposition of the dynamic limit values of both axes systems.
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MACHINE VISION AIDED DESIGN FOR REMOTE LASER PROCESSING

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Abstract

Galvanometric scanners have become a frequently chosen industrial solution for remote laser processing of a wide range of materials. The scanners bring great advantage over conventional kinematics by delivering outstanding dynamics and processing speeds. That enables utilization of high power lasers and production volume increase by minimization of processing times. However, in batch production, as well as in small series or R&D tedious workpiece positioning and use of fixtures draws the limits on time saving.

Here, we introduce developed modules for CAD/CAM systems, tailored to support remote laser scanning systems by enabling process design “directly on the sample”. The presented solution uses orthogonal workspace imaging and raster vectorization for aided geometry fitting and adjusting process trajectories to the real position of workpiece. The developed algorithms bring flexibility and integrate designing stage with process realization. The system can adapt execution task to misaligned workpiece and still keep defined precision. This minimizes operation times and process failures. System errors were estimated experimentally on geometrical primitives. As a system validation, two industrial use-cases are presented, where material surplus removal is required. Overall system performance meets these tasks’ accuracy requirements. The work focuses on research environment and gives basis for industrial architecture as further development.

Keywords: remote laser processing, CAD, machine vision, galvanometric scanners, image processing

1 Introduction

Remote laser treatment is applied e.g. in welding, heating, engraving, cleaning and cutting of metal and non-metal materials [1, 2]. Motivation for the industry to use galvanometric scanners in laser processing was the extension of production throughput by significant increase of speed.
and working field. Beam positioning by deflecting mirrors and large distance to the workpiece from scanning head allows to achieve processing speeds even over 800 m/min [3]. In comparison, conventional laser systems based on CNC or robot kinematics achieve speeds of tens of meters per minute. Higher processing speed also reduces heat-affected zones (HAZ) due to shorter material interaction time. This is a key factor e.g. in carbon-fibre-reinforced polymers (CFRP) cutting, where reduction of thermal influences is relevant [4].

However, scanning systems have also their drawbacks, which limits the mentioned benefits. Some of the main problems of remote processing are the following:

- lack of direct, easy to reach, reference elements for position measurement,
- time consuming positioning at process designing stage,
- difficult or not possible correction of workpiece misalignments at processing stage,
- trajectory scaling deformations, due to height change and differences in mirrors dynamics,
- natural pillow-barrel projection distortion, caused directly by scanner construction,
- complex distortions from optical elements imperfection and scanner actuators nonlinearity.

According to the problems mentioned above, precise and reliable workpiece positioning, which is essential for processing quality, causes very time consuming adjustments. In case of systems for individual parts manufacturing, prototyping and R&D, it is even more crucial to the total processing time. Also, input geometry imperfections of preprocessed workpiece can cause that ideal CAD data will not fit well to the real sample. These are both significant cost drivers.

To solve that kind of problems, industrial machine vision systems are commonly used in either lateral or coaxial setups working together with correction algorithms. There are also available camera systems for remote laser processing, however, typically offering only an image following the beam [5, 6]. It does not give real coordinates reference nor global orthogonal and scaled view of the workfield usable for measurement and trajectory manipulation algorithms. The research stage of the dedicated vision system for remote laser micro welding offering reliable trajectory designing and beam positioning has been presented and discussed by Stache et. al. [7, 8], however the functionality focuses on particular remote system and welding tasks.

As the positioning of element is a limiting factor to achieve better beam-on share in any laser remote processing, we developed modular algorithms applicable for CAD/CAM frameworks. The algorithms deliver data adjustment and trajectory designing directly on the orthogonal sample projection. This paper focuses on machine vision aided approach for designing paths for remote laser processing.

2 Machine Vision Aided Design

The software modules rely on panorama images, stitched from single image tiles acquired by self-developed coaxial imaging system for galvanometric scanners. Developing a vision solution for scanner systems that gives precise information about whole workspace geometric dimensions and location of workpiece in the machine coordinates brings specific difficulties. All acquired images have to be transformed to orthogonal projection. It has to be ensured that the position in the image equals real part position. Due to high accuracy demands, also image resolution should allow to achieve required precision. Any distortions from the camera and optical elements have to be eliminated by a calibration routine.

To enable process designing on the workspace image, several conditions must be fulfilled:
– scanner system must be calibrated,
– vision system must be calibrated,
– vision and scanner coordinate systems must be registered to a common coordinate system,
– data exchange standards and interfaces should be developed.

Other issues are connected with system ergonomics and usability:
– only specific information should be extracted and presented as visualization for user,
– designed trajectories should be adjusted according to vectorized information extracted from image,
– user should have the ability to customize set of information that is presented,
– functionality of reusing the developed extraction algorithms should be provided.

2.1 Architecture of the system

Machine Vision Aided Design module (MVAD) requires complete, stitched from tiles, and undistorted panorama image. It is delivered by the acquisition software. Acquisition parameters which are: scale (px/mm), offset vector (mm) and rotation angle between coordinate systems of the machine and the image are also delivered as an input data. The output, after post processing of designed trajectories, is in the form of a machine script. In the reported prototype system it is "rtc script" file. The overview of global system components data flow is presented in Fig. 1.

![Fig. 1. System components' interaction](image)

The designed system’s architecture is presented in Fig. 2. The acquired panorama image is loaded to the Machine Vision Aided Design core module, together with acquisition parameters as an essential information for coordinate transformations. MVAD module uses image processing algorithms to extract relevant features from the panorama image and aid software user by visualization and fitting trajectories to features during process design. The algorithms can be parameterized and stored in a serialized way. Image processing algorithms work on the original, high resolution image to hold required precision. Then, only their output is transformed to the CAD system compliant with the machine coordinates.

The developed MVAD software module can cooperate with the environment providing basic CAD functionality. For development and validation, an open source CAD systems were used. This enabled easy modifications and extending of user interface. However, an adapter module provides flawless portability to other CAD systems. To connect MVAD framework to particular CAD environment, a crafted adapter has to be developed. The framework provides abstract adapter structure, which defines implementation standards for particular CAD system adapter. The adapter is responsible for coordinate systems’ conversions and data types transformations between base CAD environment and developed Machine Vision Aided Design core module.

Elements allowing convenient, so called "directly on the sample,” process design were added to the CAD module. All user actions are handled and passed by GUI of the CAD system. Also all
the results of MVAD image processing and representation of the panorama image are translated by the adapter and presented to the user by this module.

**Fig. 2. Software system architecture**

To achieve fully integrated environment for designing remote laser processes, also CAM module was developed. Except for basic features, as setting process parameters and machine script generation, the module is connected to the technology database. That simplifies and speeds up the designing process of known materials. However, CAM module remains out of the article scope.

### 2.2 Features extraction algorithms

Feature extraction is an image analysis step for vectorization. It reduces dimensionality and removes irrelevant data. Also trajectory adjustment and paths generation relies on it. Feature extraction is realized by pre-processing and segmentation stages. As it is not possible to design a universal algorithm for each potential case, the module enables building processing algorithms from defined set of image processing operations. The edition is available from GUI and can be serialized to XML files to be reused for similar cases.

**Fig. 3. Implemented features’ vectorization algorithms**

After image processing, features are vectorized. The developed module handles line, circle, ellipse and free contour vectorization. The implemented group of algorithms is shown in Fig. 3. Marked with bold font in the figure, are the most universal ones, and therefore, they are set as default. However, as already mentioned, each algorithm result depends on the problem class. Therefore, framework provides abstract interface for implementing efficient vectorisation algorithms for particular problems. Extending available algorithms set is not complicated and
does not require software structure modifications. Current algorithms are defined in program configuration files. By default, sensors are used for geometric primitives and contours are detected by self-developed kernel search with branch optimization.

Sensors are abstract structures used for particular shapes detection by defined searching procedures. They work on segmented image after extraction of expected features by dedicated image processing algorithm. Each sensor consists of its base and detecting elements. The base determines geometry class for detection. The parameters are: search direction, searched elements class, detecting elements density and sensor size. After points’ detection, they are fitted to the expected geometry with least square fitting and outliers elimination.

Alternative approach for line and circle detection and vectorization are Hough transform methods [9, 10]. They transform a spatial data image after contour filtering to the parametric domain, where the searched features are easily detectable, what is convenient for particular shape extraction. The transformed line is represented as a point on a plane, which coordinates define values of line equation parameters: slope and y-intercept. The circle is represented as a point by its centre coordinates and radius in 3D parametric space. Edge filtering imperfection always introduces information noise and results in inconsistency of the detected contours. To minimize false detections, an operator defines the segment length range for the searched line. Also, for the circle, possible radius range is defined by the user input, via CAD module, as an initial circle drawing. That sets the limits to the third dimension of parametric space. Defined ranges narrow point searching area within Hough transform domain. It reduces detection inaccuracy and algorithm execution time. After applying Hough algorithm, from the features found, most similar to the user input are chosen.

Contours vectorization, in most cases, uses Canny filter [11], as the last pre-processing step. Then, contours are represented as pixels with defined values. Free contour vectorization task is to find connected contour defining pixels on the extracted raster image. Our search algorithm use 3x3 kernel, which travels through the image and detects connected components creating vectorized contour representation. In case of branching, a path with smaller vector angle change is chosen.

2.3. Processing trajectories adjustment algorithms

Original processing trajectory can be transformed to the real workpiece position by comparing model and real element features’ coordinates. As a model, any shape can be used, e.g. processing trajectory or element contour. It should hold enough features to determine part position and it should be clearly noticeable on the image of the workpiece.

To fit model trajectory to the real workpiece, model features, together with corresponding features on the workspace image, need to be defined. In most cases, contours or extracted primitive shapes are used. Image features can be vectorized by algorithms described in section 3.2. Choosing sufficient amount of corresponding points allows to compute the transformation. Theoretically, already 2 point-pairs are enough, however, it is highly advised to choose more features than needed, as redundant features improve the fitting quality.

Transformation computing steps:
1. Features are connected by lines. Every feature’s pair in the model and analogously with features in the image.
2. Rotations between corresponding lines are computed.
3. Scales between corresponding lines are computed.
4. Translation between model features and image features is computed.
5. Rotation, scale and translation are calculated statistically. In the simplest variant the mean value is used. In more complicated cases outliers elimination is performed.

For manual adjustment, in most cases, valid features extraction and picking is assumed. This implies no need of outliers elimination. For the complicated tasks and automated solutions statistic elimination is developed.

The calculated transformation must be applied to original trajectory. Two workflows are available. If machine script is used as a model, transformation is applied directly to this file. All the process technology properties remain the same, only trajectory points are transformed. The second option is to apply transformation on the level of CAD data and to generate new machine script using CAM module. It requires reassigning of process parameters. This workflow should be used when there is only CAD model or there is a need to change technology parameters.

2.4. Hardware base of prototype system

![Fig. 4. Hardware system setup – real prototype station; principle schematic diagram](image)

Subject software modules were initially developed on hardware prototype setup presented in Fig. 4. It consists of customized post-objective, large field, galvanometric scanner. Optical path of the vision system and processing laser source is coaxially coupled by dichroic mirror. The prototype works with 2kW power CO₂ slab laser for remote processing realization. The developed vision system acquires single image tiles, through deflecting mirrors by 3280x2748 pixel CMOS camera with appropriate lenses. The tiles are transformed to orthogonal view and stitched into a large panorama image of workspace. This image is an input data for the developed MVAD algorithms. Scan head controller, camera and software units run on standard PC.

3 Errors estimation on prototype setup

As the MVAD algorithms’ module, by purpose, integrates well into the whole process and description of each isolated algorithm uncertainty is a discussion for a large, separate scope, here, initially, an estimation of complete, integrated system errors of geometry mapping is presented. Particular hardware setup in described in paragraph 2.4.

A global error chain is presented in Fig. 5. The acquisition of input panorama image error, estimated for the prototype setup, is 80 μm. The MVAD algorithms work with subpixel precision
and their error can be assumed as 1/3 px [13]. According to the particular setup, where 6,9 px/mm resolution was achieved, the absolute error for the MVAD module is estimated as 48 μm. Note that both, input image acquisition and MVAD, depend on image resolution, which implies dependency on camera lenses, sensor resolution and field of view. Within the block of output path processing, the main influence on geometry mapping has the scanner calibration quality. The scanner was calibrated with the maximal correction error of 84 μm.

Fig. 5. General prototype system error sources' scheme

To verify error assumptions described above, an experimental routine was carried out, based on processing circle geometric primitives and measuring their centres’ displacements. A test matrix of 7x7 circular patterns covering a 260x260 mm square workspace was proposed. Each circular pattern consists of inner and outer circle edge, with 14 mm and 16 mm radius respectively. The inner circle is a further base reference for measurement, while the outer circle is the subject of complete processing routine. The patterns are numbered to reproduce their location in the workspace after cutting process. The complete test matrix was prepared on a 240 g/m² basis weight paper.

The routine starts with placing the matrix in the workspace in the way to obtain correlation of matrix orientation and system coordinates. This allows to estimate spatial error distribution within the workspace. Both, the scanner itself and the input image acquisition system were initially calibrated as described earlier. The panorama image of the test matrix was acquired and loaded to the MVAD module, together with transformation information (Fig. 6). A feature extraction preprocessing algorithm was adjusted and applied. Then, detection and vectorisation of circles was done by the sensor based algorithm. Processing trajectories were generated and following...
technology parameters were assigned: scanning speed $v_s=1.5\,\text{m/s}$, laser power $P=165\,\text{W}$, cw mode, number of repetitions $rep=8$. After cutting, samples were collected for measurements.

For each circular pattern, the measurement of referential and processed circle centres’ displacement was carried out. To bring sufficient precision, the measurements were done on the optical, telecentric setup, as specified in Table 1. The setup was previously calibrated to minimize residual distortions. For repeatability and automation, a simple measurement software was developed in LabView (Fig. 7a). Each circle was detected by 3600-element sensor and its centre was determinate as the mass centre of the detected figure.

**Table 1. Measurement setup for resulting geometry verification**

<table>
<thead>
<tr>
<th>Camera</th>
<th>2/3” CCD, 2452 x 2054 pixel</th>
</tr>
</thead>
<tbody>
<tr>
<td>Optical lenses</td>
<td>Bi-Telecentric lens, telecentricity $&lt;0.08$ deg, distortion $&lt;0.07%$, CTF @ 70 lp/mm $&gt;50$, magnification 0.137x, FOV $=64 \times 48$ mm</td>
</tr>
<tr>
<td>Illumination</td>
<td>backlight illumination</td>
</tr>
</tbody>
</table>

![a) Single circular pattern - measurement application screen b) The map of displacement error distribution over 260x260 mm scanner workspace](image)

The mean displacement distance value over the whole test matrix is $d_{err}=120\,\mu\text{m}$, with standard deviation of $61\,\mu\text{m}$. In Fig. 7b., the displacement error distribution, over 260x260 mm, scanner workspace is shown. The distribution corresponds to the scanner calibration errors.

4 Industrial use cases

4.1 Generating processing trajectories from vectorized features

An example of a real utilization of the system with the MVAD trajectory generation algorithms is presented. The so called “ISO grid” is a stress adapted lattice structure of carbon fibres, flooded in a layer of the epoxy resin. The lattice is created with the Tailored Fibre Placement (TFP)
technology. Each element can differ in shape and dimensions, due to earlier production stages tolerances. The task is to remove resin surplus from the inside of the lattice structure.

Fig. 8. a) “ISO grid” lattice placed in workspace, b) acquired input panorama image, c) result of edges’ (features) extraction d) part of acquired panorama after first processing cycle, e) final processing result. Material from Leibniz-Institut für Polymerforschung Dresden e. V.

The realized remote laser cutting process steps, supported by the MVAD trajectory generation, are presented in Fig. 8. After contours vectorization, CAM module was used for machine script generation. Additionally, 1 mm offset and laser beam diameter compensation was defined. Fig. 8d shows the elements after the first process cycle run and Fig. 8e shows the final result. The “ISO grid” material comes from the Leibniz-Institut für Polymerforschung Dresden e. V.

For this use case, the estimation of accuracy errors was performed. The test lattice had 36 triangular-like resin gaps to remove. Two lattices was processed. The image acquired again by the system after the process, was used for measurements.

Two variables were taken for measurement:
- the distance between mass centres of the cut contour and the detected contour, marked as $cc$ in Fig. 9,
- the distance between points of the cut contour and the detected polygon contour, marked as $dist. k$ in Fig. 9.

Fig. 9. Measurements of “ISO grid” sample
The results of accuracy error estimation are presented below, in Table. 2. For the applied offset, the TFP carbon fibre material was not damaged in any place. The results validate that the cut contours were designed and positioned correctly with assistance of the MVAD algorithms.

Table 2. Accuracy measurements for ISO Grid

<table>
<thead>
<tr>
<th>Element</th>
<th>Distance mass centres [µm]</th>
<th>Cut contour to designed trajectory distance [µm]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Average</td>
<td>Stdev</td>
</tr>
<tr>
<td>I</td>
<td>124</td>
<td>46</td>
</tr>
<tr>
<td>II</td>
<td>157</td>
<td>83</td>
</tr>
</tbody>
</table>

4.2 Adjusting processing trajectories by features matching

The second validation use case focuses on transforming the existing, preloaded trajectory to the real element position in the workspace. The element is a TFP bicycle brake booster part made of carbon fibre flooded in epoxy resin material also from Leibniz-Institut für Polymerforschung Dresden e. V. The task for remote processing is to precisely remove epoxy resin surplus from CFRP structure, as it is one of important production process steps. The element shape makes it difficult for positioning.

The features detected on the input panorama image are shown in Fig. 10a. These features were used for preloaded model trajectory fitting in a way described in paragraph 2.3. The task was performed manually, by choosing corresponding features sets from the detected in the image and from the model. Fig. 10b presents trajectory after adjustment algorithm. The ready element, after material surplus removal, is shown in Fig. 10c.

Fig. 10. Bicycle brake booster part, a) features detection, b) trajectory adjustment, c) real part after processing. Material from Leibniz-Institut für Polymerforschung Dresden e. V.
5 Conclusions

The developed modular Machine Vision Assisted Design algorithms for remote laser processes enable precise process design directly on the orthogonal view of scanner’s workspace. The operator can extract certain workpiece features by image processing algorithms. The features can be vectorised and used together with the developed CAM module for direct trajectory generation or for adjustment of already existing trajectories, which eliminates tedious positioning. That gives high design flexibility and very good integration with the processing stage, what can lead to better reliability and time saving in processing individual parts, small series and prototyping. The error estimation carried out on geometry primitives defines the positioning accuracy on the level of 120±61 μm for particular, presented scanner setup. However, it strictly depends on scanner system and vision system calibration. The two presented exemplary applications validate practical utilization and potential of the solution.

The developed algorithms and modules can be also used in automated applications. Further works, which are currently in progress, contain autonomic system operation. The processing and fitting algorithms, developed and tested in the presented R&D environment can be adapted to this solution. This will introduce fully automated system for remote laser processing without the need of workpiece positioning.

Acknowledgments

This work was carried out as a part of the “Integrated coaxial process monitoring for remote processing with high power CO2 laser – RemCoVis” project, realised in a partnership cooperation of Wroclaw University of Technology (Wroclaw, Poland) and Fraunhofer – Institute for Material and Beam Technology IWS (Dresden, Germany).

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ABERRATIONS INDUCED BY HIGH BRIGHTNESS LASERS

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Abstract

The increased brilliance of solid state lasers during the last years creates a new challenge in guiding and forming the laser beam [1]. The thermal lensing can be fixed as the dominating issue [2].

This paper discusses impacts to process results, illustrates common and new possibilities to measure aberrations and identifies possible solutions. A descriptive experiment evaluates the effect of a state of the art optical setup influenced by a high brilliant laser, regarding to changes of the welding depth. The inertial point to improve is determined by the exact timed marked knowledge of aberrations. The implemented solution, based on a wave-front-sensor, is explained in detail. [3]

The interaction of the new measurement method with standard results gives a deep view to the behavior of single optical elements. Further investigations deliver the influences of substrate material, substrate clearness and AR-coating. Finally, the connection of multi-kilowatt single-mode fiber lasers and the new measurement device allows the improvement of optical elements in a new way.

Keywords: wave-front-sensor; focus-shift; optical aberration; online measurement

Introduction

The flexibility of fiber-coupled laser systems, high laser power and beam quality of disk and fiber lasers allow a lot of new applications [4],[5]. In this context the thermal induced focal shift is the most discussed problem. This is caused mainly by the beam forming elements. The emitted divergent beam has to be focused to the work piece area, therefore a two-step beam forming is used. In the first step the divergent beam is collimated to a parallel one. In the second step the beam is focused regarding the work piece requirements to the process area. This will allow the costumer to switch between different optical setups, by exchanging one of the units to achieve the needed beam-propagation. The beam forming by using mirrors is well known from CO₂-laser applications. These mirrors are made of copper and allow an excellent cooling.
Thermal problems of beam forming mirrors are mostly solved. Switching to a ten times shorter wavelength requires a ten times better surface of the element. This should be possible by state of the art machines, but will cause higher production costs. The combination of refraction index and geometric shape creates shorter focal length and lower production costs for transmissive elements. Further a deflection is not necessary. This allows a compact design and avoids optical aberrations of higher-order terms. This is the main reason, why lenses are preferred to focusing mirrors.

A simple experiment demonstrates the influence of the thermal induced focal shift. Therefore a state of the art optic was used. Collimator and focusing unit are made of fused silica and have a doublet design. Both elements have a focal length of 120 mm. This optic is state of the art in fiber laser processing. The used laser source is a fiber laser IPG YLR 1000 SM. The thermal induced focal shift is time-depended and the estimated response time for a thermal stabilized system is approximately two minutes. If the influences should be demonstrated in one welding seam, a large work piece is needed. We choose a stainless steel (1.4301) pipe with a wall thickness of three millimeters and an outer diameter of 50 mm. The welding of a helix structure allows a long welding seam in a small work piece without any interruptions. When we are using 1 kW laser power, the optimal feed rate is 4 m/min. The focal point was determined by industrial established equipment. Figure 1 shows cross sections for a twelve meters long welding seam, every 157 mm respectively every 2,35 seconds.

![Image]

**Figure 1: Periodic cross sections for a 12 m long welding seam**

In part A (first minute) an increasing of the welding depth can be seen. Part B (second minute) shows an area of root fusion. In part C and D the welding depth decreases. How does the focal shift correlate with the welding depth? The welding depth was detected by the Primes MicroSpotMonitor. Usually, such a measurement takes one minute. The working point was set to this value. The focal shift for glass lenses reduces the effective focal length, which provokes a wrong (greater) value for the process start. For the comparable point of time (measurement and process), we have root fusion. The measured focal point correlates to the distance to the work piece surface. Later the focal length gets shorter, which forces a reduction of the welding depth.
The laser power induced focal shift changes the focal point continually. Normally a laser welding process takes seconds and has laser-off time for cooling down, which reduces the effect. Further the chosen setup has a theoretic Rayleigh length of 145 μm, which emphasizes the effect extremely. This experiment shows the influence of the focal shift in a laboratory environment. The topic of contamination in the production environment affects the result, additionally. Typical processes have an overage of laser power to avoid this problem. If this overage is reduced by the process laying-up, the focal shift influences the quality of the process result. This experiment is the starting point to evaluate existing optical elements regarding to aberrations, developing new measurement tools and detecting points of improvement.

Start-up investigations

The typical focal length for fiber laser applications can be termed from 100 to 500 mm. The optical magnification should be between 0.5 and 2. This will prevent the aggravation of the imaging quality. Commercial elements are available in diameters from 20 to 50 mm. The use of large beam diameters will reduce the gradient of thermal load and will influence aberrations, positively. In sum, a collimation length of 100 to 200 mm is the best choice for most applications.

As a first step the investigations are reduced to the focusing unit. The collimator influences the focal shift of the complete system by its input. The squared magnification of the complete system is the weighting for this. One chosen measurement collimator reduces the cross influences of this element. The measurements for all focusing elements are realized by this collimator. Further the same focal length for the test objects reduces the influence of the weight. The chosen test objects differ in the supplier, which entails different substrates and coatings. All elements have a focal length of 200 mm. The data sheets mention fused silica for the substrate material and a reflection less than 0.2 percent. The request for further information delivers details about the substrate material, but gives no information about the non-reflective coating.

After purchasing the elements, the question was how to measure them? The available measurement equipment is divided into two methods [7]. The principal of serial scanning takes a lot of time and allows no online measurement. This concept renounces attenuating elements, which reduces the measureable power density. The camera based method, using a CCD-camera, allows an online measurement, but adulterates the result by attenuating and imaging elements, which are although influenced by the high laser power. Two drawbacks can be fixed to these measurement methods. Generally, the high power laser beam is used for measurement and influences the result. The absence of a real time measurement of the scanning system, rather the attenuating and imaging elements of the camera based system do not allow a comparable benchmark of a single optical element. It allows estimating a complete system consisting of laser, collimating unit and focusing unit. These measurements are limited to few operating points and allow no conclusion about the point for optimization.
These are industrial proved systems and can be used to collect first important information. The comparability is secured by identical measurement proceedings. The measurement starts after exactly two minutes and takes twenty seconds. The temperature of the lens was checked before starting a new measurement. The cool down of a lens, loaded with one kilowatt laser power, takes more than 10 minutes.

Figure 3 shows the measurement results for a selection of different non coated elements. In general a longer focal length will create more focal shift than a shorter one. The normalizing to the Rayleigh length balances this and enables the comparison to other focal length.

The assumption to high focus shifts of BK7 elements is confirmed. These elements are not useable for fiber laser applications. Further the differences between fused silica elements are interesting. The suppliers information should offer comparable lenses. To understand the differences in detail, a deep look into the reasons for laser power induced aberrations is necessary.


**Focal shift Background**

Lens material, no matter if fused silica or another one, has a minimal absorption for wavelength of one micron. The same effect can be noticed for anti-reflex coatings. Furthermore material properties like specific heat capacity, thermal conductivity, scatter coefficient and cooling effects generate an individual thermal distribution $T(x, y, z)$. The refraction index depends on the substrate temperature and the material coefficients. This entails a fluctuant refraction index in the lens. Additional the thermal linear expansion coefficient causes a curvature of the lens shape. They are well known as thermal optical and photo elastic effects [9].

![Diagram of thermal lensing](image)

**Figure 4: Thermal lensing**

The quality of substrate material is characterized by contamination, inclusions and OH-ratio. The contamination depends on the raw material for the smelting. High quality glasses achieve values far below one ppm. The raw material although affects the size of inclusions. Further the producers of high quality glasses try to reduce the OH-ratio by new production processes. The measurement of glasses with different OH-ratios shows no influence between 1 and 1000 ppm. This seems to be not the dominating issue. Rather the quality of the raw material impacts the absorption behavior mainly. The determined absorption of fused silica is less than one per mill. The temperature-sensitivity regarding the refraction index is $10^{-6} \text{ K}^{-1}$ and the linear expansions coefficient $0.55 \cdot 10^{-6} \text{ m K}^{-1}$ [9]. The measured focal shift for uncoated lenses cannot only be explained by these parameters. The expected shift should be smaller. The material constants can be used for a theoretic view. In reality it is influenced by environment conditions. The most interacting point is the contamination of the elements. This is changed permanent and cannot be detected reliable. This surface effects can observed for coated elements, too. The comparison of the same element without and with different coating delivers different results. The trend shows an increasing of the focal shift for coated elements.
Deep matter investigation

Normally the working laser is used for measurements, which influence the result. A new idea is the use of the working laser for the thermal load of the device and the measurement with a low power laser. The low power does not influence the measurement result. Further an online measurement does create new knowledge of the focal shift behavior. This can be done by a wave-front-sensor (WFS) regarding to the Hartmann-Schack principle. This method is well known for measurements of surface curvatures or even to characterize the figure of merit of lenses without high laser power. The illumination generates a parallel wave front, which can be corrected by a calibration surface. The exchange to the measurement device allows the calculation of the change compared to the calibration. Afterwards the changes caused by the laser beam can be measured.

The sensing element consists of a micro-lens-array and a CCD-Camera. The vectors of the micro lens array allow fitting the thermal changes to Zernike polynomials. Such a WFS is designed for an illumination power in the range of some milliwatts, so the main task is the protection of the WFS to the high laser power.

Therefore two setups were tested. Setup 1 is divided in the high power laser beam and the measurement beam. The two beams are working at an angle of 10 degrees. This allows separating the measurement beam from the laser beam and the protection of the WFS. The relative method allows measuring the difference between laser influenced and non-influenced element. By using a fast firewire camera interface and special binding algorithms it is possible to record a large amount of data in a short time. After that, the variation for every time step can be calculated. The pretesting of different WFS systems detected the optimal combination of CCD-camera, illumination, micro lens array and telescope. Because the measurement is influenced by the air flow of the environment, the whole measurement device gets capsulated. The physical setup can be seen in Figure 5.

The Zernike polynomial \( R_2^0(z) = z^2 - 1 \) presents the defocus of the element. A simple series connection of nominal focal length and thermal focal length delivers the real focal length. The wavelength difference between laser and illumination can be considered by a correction factor. [6]
For the following tests the same lenses from the start-up investigation were chosen. The exemplary results of fused silica lens 3 are demonstrating the performance of the developed device. Regarding to the gone measurement a first period of 85 seconds was fixed. The sampling rate was defined to 5 Hz. Every laser power was measured three times. The root means square deviation of the results is less than 2.7%.

The measurement of the time-dependent thermal induced focal shift can be realized easily. Higher order aberrations are more interesting for reflective optics. The inhomogeneous warm-up of a lens causes an inhomogeneous distribution of the refraction index. In the result the focal point gets larger by spherical aberration. Figure 8 shows the measurement of the same lens at 400 watts laser power. Expectedly, the value for spherical aberration is rising and coma and astigmatism are not influenced.
The measurement setup of Figure 5 has two drawbacks. One of the two beams has to operate under an angle to separate the beams. Further the beam propagation do not follow the design requirements. The working laser enters the element in a collimated geometry and gets focused. The measurement laser is collimated by the element. This setup is applicable for single elements. If a lens system is investigated, the measurement region differs to the thermal loaded region. This problem can be avoided by the measurement setup 2 of Figure 9.
Conclusion

The developed devices allow the measurement regarding to the design requirements of the optical elements and a time-depended view on the optical aberrations. The functionality was demonstrated by several elements. Most of the measurements were charged by end users. The conference presentation will include the latest measurements and give a guideline in choosing the right elements.

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HIGH SPEED MICRO WELDING OF GLASS AND SILICON

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Abstract

Joining of glass and silicon is often needed in integrated circuit packaging. Methods like anodic bonding and fusion bonding are used for joining silicon and glass wafers currently. Compared to these methods, laser joining can provide higher flexibility using easily defined bond regions that are fast to produce and only localized heating can be used. The whole wafer is not exposed to high temperature or electrical field in the laser joining, which is very important in certain applications. We were able to produce good quality welding seams that were less than 20 µm wide. A picosecond pulsed fibre laser was used in tests and processing speeds from 100 mm/s to 2 m/s were tested. The high processing speed makes the laser joining a competitive method, as a whole wafer can be processed in sufficient time.

Keywords: silicon, glass, laser, welding

1 Introduction

Microelectromechanical systems (MEMS) have grown very rapidly and are used widely in different industries. Packaging and encapsulation of these devices is important to maintain a controlled environment for example hermetically sealed or pressurized area. Important aspect is also to prevent contamination and damage as well as achieve integration of materials [1]. One of the most common materials combination in microsystems technologies is the silicon-glass couple. It is possible to integrate electronic circuits in silicon and there exists wide variety of techniques for structuring silicon. Glass provides electrical insulation, is optically transparent and chemically durable [2].

Several different methods are used for MEMS packaging by bonding glass and silicon parts together. Direct/fusion bonding technologies require high temperature that is generally higher than 1000 °C. These technologies have been used in many types of MEMS, but mostly in silicon-on-insulator (SOI) and silicon and silicon bonding. Anodic bonding method has been widely applied in MEMS manufacturing. The anodic bonding requires a high electrical field in the range of 800-2000V to achieve high quality bonds. Temperature is lower (in range of 200-400 °C) in anodic bonding compared to direct/fusion bonding. The high voltage or temperature in these methods may deteriorate or damage MEMS devices that are stress, chemical or heat sensitive [1]. Gluing is one alternative for bonding without the need of high
voltage or temperature. Binding with glue reduces the surface requirements of the wafers, but is prone to aging [3] and hermetic sealing is difficult to acquire.

Another method for bonding is laser transmission welding. The laser beam goes through the transparent glass and is absorbed in silicon. The laser is used to produce heat in small defined area where a welding seam is wanted. This minimizes possible thermal damage to nearby structures. Because temperature is increased only near the welding seam and no voltage is applied to materials, many of the disadvantages of previously mentioned bonding methods can be avoided.

In this paper, we demonstrate the micro-welding of glass and silicon using high processing speed and ultrashort laser pulses. The ultrashort laser pulses enable precisely control heat input. The high processing speed allow fast production rate that is required by the industry. We demonstrate 10 - 100 times faster processing speeds that are used in some of the previous experiments [2,4].

2 Experimental

Joining of materials was done using a precise machine designed for laser micromachining. The machine had three linear axes and up to 2 m/s velocities were achievable. The machine was capable of under ±5 μm movement accuracy. A confocal sensor and an optical microscope were included in the workstation for positioning and inspection purpose. A pulsed fibre laser was used in all tests. Up to 5 μJ pulse energy and repetition rates from 1 MHz to 4 MHz were used in tests. The central wavelength of the laser was 1064 nm and the pulse length was approximately 30 ps.

Silicon and glass wafers were cleaned using RCA1-method to remove organic or inorganic contaminants from the wafers’ surfaces. The cleaning is important to remove any particles that might get lodged between the wafers and locally prevent bonding. [5] After cleaning materials were brought in intimate contact. In this case, adhesion between two substrates occurs due to low energy molecular bonds (e.g., generated by van der Waals forces), allowing just pre-bonded substrates' handling [6], but the bonds are not strong enough for the final applications.

Two different size samples were used. 150 mm diameter wafers and square samples that had 20 mm x 40 mm area. Two different glasses were also used in tests: fused silica and borosilicate. Some of the silicon wafers had 1 μm thickness oxide layer. Other silicon wafers had no coatings. In the silicon there were also etched areas that were used to evaluate if created welds can form hermetically sealed structures. Some of the samples and etched areas can be seen in Figure 1.
Pre-bonded samples were placed either in a vacuum chuck or in a vacuum chamber. When using the vacuum chuck, the bottom of the sample is firmly attached to the top surface of the moving vacuum chuck. This prevents unintentional movement of the sample during laser processing. The vacuum chamber also attached sample firmly in place. In addition to that it provided force that was pressing materials together. Top surface of vacuum chamber had window made from glass that allowed laser beam to travel through the glass. The glass window also pressed the pre-bonder sample under it creating the pressing force. Test results made with the vacuum chamber or the vacuum chuck had no clear difference.

Velocities from 100 mm/s to 2000 mm/s were used in processing tests. Laser repetition rate was changed from 1 MHz to 4 MHz and pulse energies from 2 \( \mu \)J to 5 \( \mu \)J were used. Optical microscope was used to inspect the welds.

3 Results and Discussion

Microscopic images were taken from processed samples. The processing tests included different material combinations. Processing velocity, laser power or repetition rate were changed. Tables 1 to 4 show some of the images taken. The microscope settings were not the same in all images. This results to for example different background colours in the images taken from samples that include oxide layer.

Table 1 shows images taken from different material combinations. Both the glass type and the presence of oxide layer had clear impact on the welds. While using same processing parameters, slight differences in the welds between the borosilicate and the fused silicate were expected since thermal properties of the materials differ. The oxide layer had greater impact on the welds and there can be seen clear differences in the welds with and without the oxide layer. The welds in samples where the oxide layer was present are less uniform and especially the weld edges are more rugged.
Table 1: Processed lines in different material combinations using 3.5W average power, 3.5\(\mu\)J pulse energy and 1 MHz repetition rate

<table>
<thead>
<tr>
<th>Material Combination</th>
<th>(v) (mm/s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Borosilicate + Si</td>
<td>100</td>
</tr>
<tr>
<td>Fused silica + Si</td>
<td>200</td>
</tr>
<tr>
<td>Borosilicate + Si (1(\mu)m oxide layer)</td>
<td>300</td>
</tr>
<tr>
<td>Fused silica + Si (1(\mu)m oxide layer)</td>
<td>400</td>
</tr>
<tr>
<td></td>
<td>500</td>
</tr>
<tr>
<td></td>
<td>600</td>
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<td>700</td>
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<td></td>
<td>800</td>
</tr>
<tr>
<td></td>
<td>900</td>
</tr>
<tr>
<td></td>
<td>1000</td>
</tr>
</tbody>
</table>

In all material combinations, same kind of behaviour can be seen when the processing speed is changed. Table 2 shows impact of pulse energy and processing speed in borosilicate glass and silicon. The lowest processing speed of 100 mm/s caused periodic changes in the welds. The welds were discontinuous and uneven when using this processing speed. When the processing speed was increased, fairly smooth and even black line was formed. Increasing velocity even further caused the welds to turn granular and lighter in colour in the microscopic pictures. The processing speed when changes in weld formation occur depends on laser power and frequency.
Table 2 Processed lines in borosilicate + Si, using different average powers and 1 MHz repetition rate

<table>
<thead>
<tr>
<th>Power (W)</th>
<th>Repetition (µJ)</th>
<th>v (mm/s)</th>
<th>100</th>
<th>200</th>
<th>300</th>
<th>400</th>
<th>500</th>
<th>600</th>
<th>700</th>
<th>800</th>
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</tr>
</thead>
<tbody>
<tr>
<td>2.0 W</td>
<td>2 µJ</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.5 W</td>
<td>3.5 µJ</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>5.0 W</td>
<td>5.0 µJ</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Effect of the laser power can be seen in Table 3. Laser repetition rate and processing velocity was kept constant, and laser average power was increased from 3.5 W to 6.9 W. After the laser processing, samples were diced and microscopic images were taken from cross-sections. Each of the six images show cross-section of six different lines with identical processing parameters and 10 µm line spacing. From the cross-sections, effect of the laser power in weld formation can be seen more clearly compared to the images taken from topside. When lower laser power values of 3.5 W and 4.1 W were used, there were only slight mixing of materials and the welding seams were hardly noticeable from the base materials. Increasing the laser power caused stronger mixing of the materials and the welding seams were clearly distinguishable in the images. When laser powers of 5.5 W and 6.2 W were used, clear differences in the weld cross-sections can be seen even with identical processing parameters. In some of the weld cross-sections, anchor shaped seam can be seen that was probably caused by recoil force of evaporation. The highest laser power of 6.9 W caused strong mixing of the materials and the seam height was considerably higher than in lower power levels.
Table 3 Cross-section of processed lines in borosilicate + Si, using different laser powers and 2MHz repetition rate and 1000 mm/s velocity.

<table>
<thead>
<tr>
<th>Laser Power</th>
<th>Repetition Rate</th>
<th>Energy Density</th>
<th>Image 1</th>
<th>Image 2</th>
</tr>
</thead>
<tbody>
<tr>
<td>3.5 W</td>
<td>1.75 μJ</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>4.1 W</td>
<td>2.05 μJ</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>4.8 W</td>
<td>2.4 μJ</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>5.5 W</td>
<td>2.75 μJ</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>6.2 W</td>
<td>3.1 μJ</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>6.9 W</td>
<td>3.45 μJ</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 4 Processed lines in fused silica + Si (1 μm oxide layer), using different repetition rates.

<table>
<thead>
<tr>
<th>Laser Power</th>
<th>Repetition Rate</th>
<th>Energy Density</th>
<th>Image 1</th>
<th>Image 2</th>
<th>Image 3</th>
<th>Image 4</th>
<th>Image 5</th>
<th>Image 6</th>
<th>Image 7</th>
<th>Image 8</th>
</tr>
</thead>
<tbody>
<tr>
<td>16 W</td>
<td>4 MHz</td>
<td>4 μJ</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>12 W</td>
<td>3 MHz</td>
<td>4 μJ</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>8 W</td>
<td>2 MHz</td>
<td>4 μJ</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Velocity(mm/s)</td>
<td>300</td>
<td>400</td>
<td>500</td>
<td>1000</td>
<td>1500</td>
<td>2000</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
Table 4 shows effect of pulse repetition rate and processing speed while pulse energy is kept constant. The pulse repetition rate has significant influence in the weld formation. As the repetition rate increases, also the average laser power increases. The welds became irregular, when the higher repetition rates of 3 MHz and 4 MHz were used even with fairly high processing speeds of 300 mm/s and 400 mm/s. Increasing pulse repetition rate allowed to produce welds that look similar in the microscope images than the welds made with lower repetition rate and processing speed. For example the welds processed with 3 MHz, 4 μJ, and 1000 mm/s – 2000 mm/s in fused silica + Si looked similar than the welds made using 1MHz, 4 μJ and 300 mm/s – 400 mm/s parameters.

4 Conclusions

We were able to produce good quality welding seams using high processing speeds. Stress and thermal damage can be minimized, because material is only heated in very narrow area near the welding seam. The localized heating and absence of high electric field enables processing of devices that might be damaged when using common methods like anodic bonding or fusion bonding.

The microscopic images show that uniform welding seam without any visible defects can be achieved with all tested material combinations. When processing speed is increased, then also average laser power should be increased to produce similar welds. After approximately 2-4 μJ, increasing pulse repetition rate (compared to increasing pulse energy) seemed to give better results when higher average laser power was needed. The laser power had clear effect on the weld height, form and mixing of materials.

More tests as for example leak and shearing tests are needed to verify weld quality and to get more information how changing processing parameters affect different characteristics of the welds. These results already indicate that silicon-glass laser welding is promising method that allows high production rate and to avoid some of the shotcomings of commonly used methods in IC-industry.

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Acknowledgements

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LASER WELDING OF POLYMERS, COMPATIBILITY AND MECHANICAL PROPERTIES

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Abstract

Laser welding of polymers is today a commonly used industrial technology. It has shown obvious advantages compared to e.g. adhesive bonding in terms of higher productivity, better quality and easiness for automation.

The ongoing development of lasers tailored for polymer welding in coordination with the development of related absorbers added to the polymer materials provide the possibility of joining transparent and non-transparent materials. The automotive industry, the medical device industry and the electronic industry are just some of the areas where the technology is widely implemented.

There is an increasing industrial interest in joining dissimilar polymers. To overcome the challenges involved increased focus is set on the understanding of joining mechanisms, morphology and molecular structure behavior. Also the understanding of resulting mechanical and thermal properties is presently subject for research and development.

This paper presents some research results related to laser welding of various polymer materials, including weld compatibility investigations related to the joining of different polymers. Theory for bonding mechanisms, strength development, mechanical properties testing and other analysis methods are also highlighted.

Keywords: Laser welding, bonding mechanisms, plastics, polymers, weld compatibility, mechanical properties.
FORCE Technology has carried out investigations in laser welding of thermoplastic polymer materials within the framework of a Danish national project ‘Expanding the Weld Compatibility of Plastics’; a collaboration between a number of Danish industries and R&D institutions. These include NOVO Nordisk A/S, Coloplast A/S, FORCE Technology, The Technological Institute, University of Copenhagen and the University of Aalborg. The project was partly funded by ‘The Danish Strategic Research Counsil’. The main part of the project was focused on investigations of the weld compatibility of different polymers as well as on the understanding of related bonding mechanisms.

**Introduction to laser welding of polymers.**

The very first activities related to laser welding of thermoplastic polymers was carried out in the early 70’s, (13). With the CO2-laser it was demonstrated that 100 micron polyethylene films could be welded in an overlap joint. However, the breakthrough for laser welding of polymers occurred in the mid 90’s as stable diode lasers were developed in the wavelength range 800-1100 nanometers at power levels up to app. 200 watt. Typical weld range power level is 10 to 50 watt. The diode laser is typically integrated with a mirror based beam scanning system or a robot based flexible production system. The laser beam may be delivered directly from the output of the laser source (direct diode) or through a fiber optical cable (fiber coupled diode). Today, there are 10-20 diode laser suppliers on the market delivering equipment dedicated to polymer welding. Also other types of lasers as the Nd-YAG laser, the disc-laser and the fiber laser are entering this processing segment.

The basic principle in laser welding of polymers is illustrated in fig.1. An overlap joint type is the basic joint configuration for welding polymer materials. The ‘upper’ part of the joint is a laser light transmitting polymer and the ‘lower’ part is a laser light absorbing polymer. The ability to absorb the laser light is due to chemical dyes or pigments also called ‘absorbers’, which are added to the polymer before moulding. The most used absorber is carbon black, but many other dyes or pigments may be used. In another welding approach an invisible, but infrared liquid absorber additive is simply supplied between the surfaces just before welding - this liquid absorber is sold under the trade-name ‘Clear Weld’, (14). During welding the laser beam energy is absorbed in the interface between the material surfaces. As the materials starts melting, the surfaces are welded together in a narrow region around the joint.

Due to the mentioned characteristics of the welding principle the weld is located ‘inside’ the material, in a similar way as known from resistance spot welding of metals. Some of the benefits related to the laser welding of polymers are therefore, the invisible joint, highly hygienic weld with no impurities, carried out at high speed with a very low heat input.

One of the big challenges related to laser welding of thermoplastic polymers is the control of the beam power absorption in the materials surfaces in the joint interface. Most polymers are usually transparent or translucent in the visible and near infrared range. Only by pigments or chemical additives, suitable absorption of the laser wavelength is achieved. A number of companies offer special infrared absorbers for different wavelength to add to the material prior to moulding or extrusion.

When laser welding, the absorber is only added into the ‘lower’ absorbing part of the joint, while the ‘upper’ part of the joint still need to be transparent.
Fig. 1 Principle of polymer laser welding. (21)

Basically, a certain polymer in a transparent condition and a corresponding polymer in an absorbing condition may be welded together without problems. However, only a limited part of different polymer types are compatible. The present project activities involve the testing of the compatibility for a various number of thermoplastic polymers as the basic work.

The optimum quality of the weld regarding strength is typically achieved at a certain line energy (i.e. energy delivered per unit length) which means a certain ratio between power level and welding speed. Too low line energy may result in only slight adhering, while too high line energy may result in material decomposition.

A number of methods for online quality control of the laser welded polymer are available on the market. Pyrometers offer the possibility to monitor the welding process online. It observes the temperature in the welding zone without contact by detecting the thermal spectrum of radiation emerging from the process. The measured temperature enables the judgment of the overall processing conditions and to spot any local irregularities along the joint, (18,19). Monitoring systems can be equipped with feedback controls for online adjustment of the laser power during welding in order to maintain a constant temperature profile. Vision systems based on Online CCD monitoring is an additional method offered by a number of companies. This method is already widely used for QA in industrial manufacturing. It can be used for monitoring the weld quality of materials that provide a sufficiently high level of contrast, such as opaque/black materials. Such a monitoring system can be easily integrated especially for contour welding processes, (18).
Development of mechanical strength in polymer welds.

Various models have been proposed for strength development at polymer weld interfaces (1,2). Basically, the objective of thermal welding of thermoplastics is to melt two polymer surfaces, bring them together, wait for them to cool down, and – if done correctly – a strong joint appears. More scientifically, from initial solid state to a high strength welded joint, five important criteria for strength development are found: Melting, wetting, compatibility, diffusion and entanglement, and possibly (co-)crystallization. These five criteria are illustrated in figure 2 and discussed in the following.

**Fig.2** A principle sketch of thermal welding of two semi-crystalline materials. Note the difference between interface and interphase, which is suggested by Pizzi and Mittal (3).

**Melting**

The melting phase is often what distinguishes the various welding methods, e.g., laser, ultrasonic, or hot tool welding. Melting of polymers is not a simple physical process. It is in fact a complicated and dynamic process over a range of temperatures and not a simple melting point (4). Therefore, during the melting phase, it is desirable to have the temperature as high as possible for as long time as possible without obtaining polymer decomposition. Polymer decomposition can be avoided using high temperatures for very short periods of time. A process capable of that is laser welding, e.g., the maximum temperature in a PA6/PA6 weld seam have been reported to 370 °C, which is 45 °C above the conventional decomposition temperature (5,6).
Wetting
After melting the first step in the establishment of mechanical strength is wetting. Wetting is necessary to achieve an intimate contact between the two molten polymers, which again enables polymer diffusion and entanglement. Whether two polymers wet one another, can be determined from Young’s equation of surface free energy (7):

\[ S = \gamma_1 - \gamma_2, \]

where \( \gamma_i \) is the surface energy of material \( i \) and \( S \) is the spreading coefficient, which for positive values infer spontaneous wetting. For instance, \( \gamma_{\text{HDPE}} = 25.3 \text{ mJ/m}^2 \) and \( \gamma_{\text{iPP}} = 19.4 \text{ mJ/m}^2 \); hence, \( S = 25.3 \text{ mJ/m}^2 - 19.4 \text{ mJ/m}^2 = 5.9 \text{ mJ/m}^2 > 0 \). Therefore, iPP is able to wet HDPE. This fact is important when selecting which material to be absorbent in a through transmission laser weld. On the other hand, for iPP and HDPE it has been proven that this material combination is weldable with both materials as absorbing (8).

Compatibility
Polymer compatibility or polymer miscibility is relevant when welding dissimilar polymers. Polymer-polymer miscibility can be predicted from Flory-Huggins (FH) theory for polymer-polymer mixtures. FH theory links enthalpy and entropy to mixing. In general, enthalpy disfavors mixing while entropy favors mixing. However, the polymer chains in commercial plastics are so long that the entropic gain of mixing is practically negligible. Therefore, it is also concluded that polymers are only miscible if their solubility parameters are practically identical (9).

This not said that dissimilar polymers cannot be welded – sometimes they can! This is explained from Helfand’s theory, which estimates the equilibrium interpenetration depth (\( w_m \)) illustrated as the green area in figure 2. If \( w_m \) is large enough entanglements can form and high-strength welds are achievable. For instance, even though HDPE and iPP are not miscible according to the FH theory, they are still weldable (10) – also with lasers (8).

Diffusion and Entanglements
If the wetting and the compatibility criteria are fulfilled, diffusion at the polymer interface occurs. Regarding diffusion, different time domains are important to emphasize; these include Rouse relaxation of entangled segments (\( \tau_e \)), Rouse relaxation of the whole chain (\( \tau_R \)), and the reptation time (\( \tau_{\text{rep}} \)) (11). The reptation time reveals the time it takes a polymer to diffuse its own radius of gyration (\( R_g \)), i.e., move its own size. When polymers have diffused one \( R_g \) across an interface, full strength is developed (1). Thus, \( \tau_{\text{rep}} \) is very important to estimate for the welded polymers. For instance, \( \tau_{\text{rep}} \) of iPP at 170 °C with a molecular weight of 237,000 g/mol has a reptation time of 3.7 ms. Therefore, although it varies inverse proportional to the temperature and with the molecular weight cubed, inter-diffusion at the interface cannot normally be the limiting factor for weld strength establishment (8).

(Co-)crystallization
Co-crystallization is central for strength establishment in semi-crystalline weld interfaces. The term ‘co’ refers to the ability of two polymers two crystallize in the same crystal phase. This has for instance been reported for LLDPE and PP (12). If co-crystallization is not possible the interphase will solidify in an amorphous state resulting in lower mechanical strength. Moreover as illustrated in figure 2, when the molten polymers retract during solidification micro-swirls can appear resulting in mechanical interlocking. These influx formations can vary in size from nano-fibrils to hundreds of microns (2).
Experimental equipment set-up for the laser welding of polymers.

A special fixturing set up was designed for experimental work at FORCE Technology. Test sample sizes up to 100*100 mm are possible to process in this equipment. The ‘upper’ and ‘lower’ part of the joint is pressed together against a glass plate by a pneumatic system. Variable pressures using up to 125 kilos is possible. The weld is performed through the transmitting glass plate. A 940 nanometer diode laser was used for the experimental work. The laser power is adjustable up to 60 watt. Fig.3 and fig.4 show the fixturing system and the technical specifications for the laser. A CNC controlled XY table was used to move the test sample at a certain speed during welding.

![Equipment](image)

**Fig.3** Test sample fixturing system and laser specifications.

![Cleaning](image)

**Fig.4** Test sample fixturing system and procedure for loading the system.
Compatibility of different polymers – welding procedure and quality analysis.

A so-called ‘weldability lobe’ was developed for the evaluation of the weldability of a certain material or for the compatibility of different materials. Sample sizes of 100*50 mm were used. A weldability lobe consists of e.g. 25 welds carried out at 5 different power levels at 5 different welding speeds. The welds are evaluated visually as well as by testing the strength of the weld. If the weld is nearly impossible to breakdown by mechanical testing in a simple tensile test the weld is approved. Succeeding, more standardized mechanical tests may reveal the exact strength of the joint.

Fig.5 illustrates two weldability lobes for the welding of PMMA and PC. In the first diagram PMMA is the transparent material and in the second diagram PMMA is the absorbing material. Good, acceptable welds are marked with blue dots in the diagrams. The size of the blue area indicates whether the material is easy or difficult to weld. As seen there is a great difference between the two diagrams. Having the PMMA material as the transparent part of the joint results in having a larger weldability lobe and in general better weld quality.

In general, it was found that if one combination revealed possible the opposite / inverted combination was as well. However, the size of the weldability lobes may be quite different. It is assumed that the reason for this is related to the difference in melting temperature ranges of the two materials. It was indicated during the experimental work that the material with the highest melting temperature range should be considered as the absorbing part of the joint to give the widest weldability lobe and thereby contributing to a more stable joining process.

Figure 6 shows a macrograph of a joint between PC and ABS. Due to the lower melting point of the ABS being the absorbing part, required overheating results in porosities due to local boiling effects. The glass transition temperature was found to be approximately 100 °C for the ABS and 149 °C for the PC.
Fig. 6  Macrograph of PC (transparent) welded together with and ABS (absorbing) and a DSC curve of PC (red curve) and ABS (blue curve), ref. FORCE Technology.

Fig. 7 shows a polymer compatibility matrix giving an overview of the results of the present work carried out. A number of polymers are listed vertically and horizontally in the diagram. The vertical list of materials constitutes the transparent part of the joint. The same materials listed horizontally constitutes the absorbing part of the joint.

The result of a certain combination of polymers is given by either a green or a red mark. A green mark means that the combination has been proven weldable by laser and that a certain weldability lobe has been created. A red mark means that the combination has been proven weld incompatible.

Fig. 8 shows the weldability lobe for some of the material combinations which were proven weldable during the experimental work using commercially available, but not well-documented materials. These comprise different combinations of both amorphous polymers (like ABS, PC, PU, PMMA, PVC) and semicrystalline polymers (like PP, PE and POM) e.g. the following combinations; ABS/PC, PU/PC, PP/PE, PMMA/PVC, PC/POM, PMMA/POM.

Fig. 7  Polymer compatibility matrix by FORCE Technology.
Fig. 8 Examples of weldability lobes for different combinations of thermoplastic polymers welded by laser.
Fig. 9 illustrates the FTIR (Fourier Transform Infrared Spectroscopy) imaging technique for determination of the involved polymers. By infrared illumination and detection, the molecular fingerprint will reveal the type of polymer investigated. It is the aim to investigate to what extent this technique may be used for evaluation of material mixing and the bonding mechanisms within the melt zone of the joint.

FTIR image of laser weld of ABS and PMMA

![FTIR image of laser weld of ABS and PMMA](image1)

Fig. 9 FTIR image of an ABS – PMMA laser welded joint.

Mechanical properties of laser welded polymer joints.

In order to evaluate the quality of the welds, two mechanical tests were applied to the welds. A simple tensile test perpendicular to the weld direction was carried out on standard tensile test equipment. A special fracture mechanical test (22) was carried out as well for comparison for selected materials.

Tensile testing.

50 mm long overlap joints were used for test samples. To avoid edge influence on the test results, a radius of 10 mm was machined in each side of the test specimen so that the effective length of the weld to be tested was 30 mm. The test specimen was mounted in the testing equipment as shown in fig. 10. The tensile test was carried out perpendicular to the weld direction including 3-5 tests per point.

Tensile results were documented via stress-strain curves as illustrated in fig. 10 as well.
**Fig 10** Tensile test specimen mounted in test equipment (left). Example of test results (right).

**Fracture mechanical testing.**

The fracture mechanical tests were also based on 50 mm long overlap joints. A steel blade is slowly pressed between the two joint materials in direction against the weld. During this procedure an increasing stress is generated in the weld and in particular around the crack tip edge leading to a certain amount of plastic deformation. At a certain position (distance, a) from the weld the stress has reached a critical level at which the weld is disrupted/broken. The fracture toughness, $G_c$, also called the critical energy release rate is an expression for the energy required for the crack to propagate and consist of a term from the bonding strength of the joint as well as a term from the energy uptake in the plastic deformation around the propagating crack tip (22), and thereby an expression for the strength of the joint. In the present tests the steel blade thickness was 1 mm and 3 tests were carried out in each point. $G_c$ may be calculated from the equation shown in fig.11, (22). Values of, $h$, refers to the thickness of the materials. Values of, $E$, refers to Young’s modulus of the materials. The test equipment is shown in fig.12.

\[
G_c = \frac{3\Delta^2E_1h_1^3E_2h_2^3}{8a^4} \left[ \frac{E_1h_1^3C_2^2 + E_2h_2^3C_1^2}{[E_1h_1^3C_2^3 + E_2h_2^3C_1^3]^2} \right]
\]

with $C_1 = 1 + 0.64h_1/a$ and $C_2 = 1 + 0.64h_2/a$. 

**FIG.11** Fracture Mechanical test. Principle and calculation of fracture toughness value, $G_c$, (22).
Fig 12 Fracture mechanical test specimen mounted in test equipment. Illustration of the a-value (left). Example of test results (right).

Results.

For a number of materials tensile strength and the fracture toughness was measured at selected welding parameters within the corresponding material weld lobe. Fig.13 illustrates the results for the materials PMMA and PVC respectively. It is observed that there is a good correspondence between the tensile strength ($\sigma$) and the fracture toughness ($G_c$). At lower welding speeds both the tensile strength and the fracture toughness seems to be marginally higher compared to welds carried out at higher welding speeds related to the material PMMA. In the case of the material PVC both the tensile strength and the fracture toughness of the welds are obviously higher in the low welding speed region. It is proposed that this is due to a longer time involved for diffusion and entanglements very close to the heat source, at the low welding speed region.

Fig 13 Results of tensile strength and fracture toughness strength tests for PMMA and PVC carried out at selected welding parameters within the related weld lobes.
As illustrated in the compatibility matrix, fig.7, a number of different polymers may be welded successfully. However, during the mechanical tests evaluation it was found that the correlation between tensile strength and fracture toughness is not as unambiguous as found when welding similar materials.

Fig.14 (left) illustrates the weld lobe for joining materials PP and PE. As seen the tensile strength of the welds are fairly equal within the weld lobe, app 37-39 N/mm. However, a larger difference in the fracture toughness test was found. In the low welding speed region the adhesion strength (1650 J/m²) was found 2 times higher than in the high welding speed range (850 J/m²).

A similar observation was found for the combination of materials PVC and PC, fig.14 (right). In this case the tensile strength was also found fairly equal within the weld lobe, app 92-106 N/mm. However, the fracture toughness was found 4 times higher in the low welding speed range (200 J/m²) compared with the high speed range area (50 J/m²) and thus the weld tends to be more brittle at higher welding speeds. Again, it is proposed that the reason due to a longer time involved for diffusion and entanglements at the low welding speed region.

As mentioned the critical energy release measured by fracture toughness testing is a combination of the interfacial bond strength and the energy absorbed during plastic deformation around the crack tip. For the tensile strength also mechanically interlocking may be important. Fig.15 illustrates a physical analog to the described observations. Having a mechanical interlock between materials of the sketched type, also shown in fig 2, a strong tensile strength may be present although the adhesion may be poor. It is obvious, that the melting time parameter must have an important influence on the degree of diffusion and entanglement in the joint interface and thereby an important influence on the fracture toughness.

Fig 14 Results of tensile strength and fracture toughness tests for different materials welded together. PP welded with PE and PVC welded with PC. Tests are carried out at selected welding parameters within the related weld lobes.

Fig 15 Physical analog to high tensile strength but low fracture toughness.
Summary of work.

FORCE Technology has carried out investigations in laser welding of thermoplastic polymer materials within the framework of a Danish national project ‘Expanding the Weld Compatibility of Plastics’; a collaboration between a number of Danish industries and R&D institutions. The project was focused on investigations of the weld compatibility of different polymers as well as on the understanding of related bonding mechanisms.

Various models have been proposed for strength development at polymer weld interfaces. More scientifically, from initial solid state to a high strength welded joint, five important criteria for strength development are found: Melting, wetting, compatibility, diffusion and entanglement, and (co-)crystallization. These five criteria are discussed.

Mechanical properties were investigated in relation to quality evaluation of the joints. The weldability of more than 50 polymer combinations has been tested. The process tolerances or the size of the processing window was expressed in a so-called ‘weldability lobe’.

When welding similar polymers it was observed that there is a good correspondence between the tensile strength and the fracture toughness. At lower welding speeds both the tensile strength and the fracture toughness seem to be marginally higher compared to welds carried out at higher welding speeds. When welding different polymers the tensile strength of the welds was found to be fairly equal within the weld lobe. However, a larger difference in the fracture toughness was found. In the low welding speed region the fracture toughness was found much stronger, and the welds therefore less brittle, than in the high welding speed range. It is proposed that the reason for this is due to a longer time involved for diffusion and entanglements very close to the weld interface at the low welding speed region.

This paper presents some of the project results related to laser welding of various polymers, including weld compatibility investigations and related quality evaluations mainly related to mechanical properties of the joints.

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WELDING OF THICK WALLED STEEL COMPONENTS WITH 32 KW LASER POWER

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Abstract
Since the commissioning of a 32 kW disc laser system ultimo 2012, the newly established company Lindoe Welding Technology, has performed weld tests utilizing up to 32 kW laser power; with welding of thick walled steel components the main focus. So far it has been demonstrated that it is possible to yield weld penetrations exceeding 40 mm in depth by one-sided welding. Also, it has been demonstrated that, even adopting laser only welding, hardness values can be controlled to conform to standardized acceptance levels. The main challenge with respect to welding quality seems to be solidification shrinkage and cracking. Preliminary test results obtained, suggest that this challenge can be mitigated.

Keywords: 32 kW laser power, thick walled welding, steel

1 Introduction
Lindoe Welding Technology (LWT) is a private R&D intensive company established medio 2012 with a main emphasis on bringing changes to the market for thick walled welding by means of high-power laser welding. A main focus for LWT is the 200 M€ market for wind turbine towers – as cost out is the mantra in the highly competitive wind turbine industry. The main argument promoting laser welding for the stakeholders in this industry is that up to 80 % cost savings per meter welded can be reached; together with an increased productivity by a factor 10 in comparison to submerged arc welding (SAW), which is currently the dominating technology used for welding of wind turbine towers. Despite that laser systems delivering 50 kW have existed since 20081, a 32 kW laser system still ranks among the most powerful in the world medio 2013. This illustrates, that it is not enough to have a substantial amount of laser power available – it must be utilized meaningfully to.

The Pipeline Research Council International (PRCI) recommends utilization of GMAW-laser hybrid welding for joining the root side pipeline steels demonstrate that laser welding technologies is industrially matured even for thick walled steel components. The fact that PRCI merely recommends the technology for root side welding, and recommends classical GMAW techniques for filling and capping demonstrates that there is still work to undertake before laser welding technology is applicable for real thick walled welding – tentatively defined as thicknesses exceeding 20 mm. Recent publications2,3 supports this suggestion, and
demonstrates that internal weld imperfections forming during solidification of the weld metal as a main challenge to address.

This paper will focus on technical findings at LWT experienced through ultra-high power laser welding of thick walled components, and will show examples of welds virtually free of internal defects.

2 Experimental

LWT is equipped with a 32 kW laser system comprising two individual 16 kW disk lasers. Via a so-called twin laser light cable (Twin-LLK), laser power from each laser can be led to the laser optics via two parallel and closely spaced cores – one from each laser. Each core has a diameter of 0.20 mm and the center-to-center distance between the cores is 0.36 mm for details see4.

The two individual and parallel laser beams is led to conventional laser optics, thereby yielding two spots on the work piece during welding. For most welding applications, particularly in the high-power range, the volume of the melt pool will have such a magnitude, that it is probed by laser intensity from both lasers simultaneously i.e. up to 16+16 kW (32 kW) laser power can be delivered in a single effective volume. Combined with the welding heads at LWT’s disposition, the described dual-spot arrangement gives a large degree of freedom of how the system can be operated. This flexibility is a big advantage in the dual spot concept.

Table 1. The dual beam concept of LWT’s 32 kW laser system

<table>
<thead>
<tr>
<th>Fiber</th>
<th>Diameter</th>
<th>Distance between fibers</th>
</tr>
</thead>
<tbody>
<tr>
<td>LLK (single fiber diameter)</td>
<td>0.2 mm</td>
<td>NA</td>
</tr>
<tr>
<td>Twin-LLK (twinned fiber)</td>
<td>0.2 mm</td>
<td>0.36 mm</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>CFO f200 (Mag. 1:1)</th>
<th>RFO C f300 (Mag. 1.5:1)</th>
<th>RFO C f600 (Mag. 3:1)</th>
</tr>
</thead>
<tbody>
<tr>
<td>LLK 0.20 mm</td>
<td>LLK 0.30 mm</td>
<td>LLK 0.60 mm</td>
</tr>
<tr>
<td>Twin-LLK 0.36 mm</td>
<td>Twin-LLK 0.54 mm</td>
<td>Twin-LLK 1.08 mm</td>
</tr>
</tbody>
</table>
The main disadvantage of the dual beam concept is that $x+x \neq 2x$. Meaning that all other parameters are fixed, the weld penetration and the weld quality differs significantly depending on whether the “2x” kW laser power is supplied by a single source laser or by two laser sources each supplying “x” kW laser power via a Twin-LLK. In figure 1 a comparison is shown between BOP welding with, respectively, 16 kW single source laser power and 8+8 kW laser power from two laser sources. In this particular experiment, it was found, that dual beam laser welding was significantly more sensitive to the surface condition of the test sample and that the yielded weld penetration was significantly less as compared to single source laser welding.

![Graph showing weld penetration vs weld speed](image)

**Fig. 1** BOP weld test, RFO C f300, spot orientation: Leading trailing (Twin-LLK), FP= -3mm, 8+8 kW (dual spot) and 16 kW (single source).

3 Experimental

3.1 Bead on plate (BOP) welding

Test results obtained by bead on plat (BOP) welding differ significantly from those obtained on actual joining by welding. The fact that BOP welding only allows vapors and plasma to exit towards the incident laser beam is found to be a contributing factor. Accordingly, LWT primarily uses BOP weld testing to get a first impression of where to establish weld parameters for operation; or as a first approximation to investigate specific weld topics. Still BOP welding has yielded interesting results. The “$x+x$ vs 2x” comparison shown above is one, and below is presented an example of, respectively, hardness values as function of welding parameters and achievable weld penetration.
**Hardness values**
The BOP weld test presented in table 2, was made with the purpose to see if the weld parameters could be controlled to yield hardness values that conforms with industrial acceptance levels; which it clearly did (typically applied acceptance levels for the hardness value is that it must not exceed HV 360).

The large solidification crack observed in the center of the melt metal is partly explained simply by volumetric effects: Molten steel takes up a volume approximately 3% than the solid steel. Further, the heated base material elongates linearly approximately 3% in the temperature range from 20-1550°C. Both effects causing some of the weld material top over the sample surface. During cooling some of this overtopping material will solidify as excess weld material. As no material is added, this material is missing further down below causing porosities and or solidification cracking. As already stated in the introduction, solving of this challenge is, in LWT’s opinion, required before thick walled welding by high-power laser technology reaches real industrial maturity. From previous experiments (details omitted) it was found that these solidification imperfections form more or less on a straight line, why their presence make the weld fail to comply with standard quality requirements.

<table>
<thead>
<tr>
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<th>Position</th>
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<tr>
<td>Laser power:</td>
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</tr>
<tr>
<td>16+16 kW</td>
<td></td>
</tr>
<tr>
<td>Spot orientation:</td>
<td>B</td>
</tr>
<tr>
<td>leading-trailing</td>
<td></td>
</tr>
<tr>
<td>Welding speed:</td>
<td></td>
</tr>
<tr>
<td>50 cm/minute</td>
<td></td>
</tr>
<tr>
<td>Focal position:</td>
<td></td>
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<tr>
<td>0 mm</td>
<td></td>
</tr>
<tr>
<td>Welding position:</td>
<td></td>
</tr>
<tr>
<td>PA flat</td>
<td></td>
</tr>
</tbody>
</table>

**Table 2. BOP welding aiming to yield hardness values that conform with standardized acceptance levels.**

<table>
<thead>
<tr>
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<th>2</th>
<th>3</th>
<th>4</th>
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<tr>
<td>A</td>
<td>245 HV</td>
<td>264 HV</td>
<td>245 HV</td>
<td>270 HV</td>
<td>285 HV</td>
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<td>B</td>
<td>264 HV</td>
<td>264 HV</td>
<td>260 HV</td>
<td>274 HV</td>
<td>264 HV</td>
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</tbody>
</table>

**Weld Penetration**
Another BOP weld test was performed with the aim to reach maximum weld penetration; in this respect LWT has managed to obtain weld penetrations exceeding 40 mm (see figure 2). Again the weld quality does not conform to basic industrial requirements due to weld imperfections formed during solidification.
3.2 Joining by laser welding

It has been surprising to experience how sensitive the quality of welds made using laser technology is to seemingly small variations in one of many parameters, such as focal position, variations of the chemical composition of base materials, groove preparation and the alignment of the components to be joined by welding. Accordingly, all developments of welding procedures at LWT are based on test specimens with welding set-up’s designed to resemble that of the final component closely. Further, we have adopted classical welding experience from the shipyard industry as backbone in the development of laser welding procedures. This vaguely formulated approach has among others resulted in the two examples, virtually free of solidification imperfections, shown below. It can be mentioned, that one of the important factors contributing in reaching these results is the flexibility, of the fiber bourn laser system, which allows utilization of the benefits different welding positions offer.

Figure 3 shows an example of double-sided fillet weld joining a 40 mm thick steel plate (S355) to a larger steel plate (S355) using 16+16 kW laser power. By visual inspection the weld passes visual requirements for internal solidification imperfections. The substantial incompletely filled groove observed to the right is merely a
consequence of the welding position: PB horizontal vertical. Hardness measurements indentations can be discerned as discrete bright spots.
In this set-up, the weld penetration obtained from each side is approximately 22 mm (the vertical carbide / sulfide stringer in the 40 mm thick steel plate marks its center).

As an example of real joining by welding a single-sided but weld joining two 50 mm thick steel plates (S355) is presented below. Welding was made using 16+16 kW laser power, and the weld position was PF upwards. In the cross section in figure 4 of fusion can be observed where indicated by an arrow. Otherwise, the visually assessed weld quality conforms to industrially applied standards.

![Weld test](image)

**Fig. 4** Weld test: RFO C f600mm, spot orientation: Side by side, FP=0 mm, 16+16 kW
The superimposed arrow point of a small weld imperfection: Lack of fusion.

#### 4 Summary

LWT has during the past six months gained experience of thick walled laser welding with up to 32 kW laser power.

It has so far been demonstrated that it is possible to control welding parameters to achieve hardness values that conform to standardized acceptance levels.
Substantial weld penetrations, exceeding 40 mm in depth, has been reached by single-sided laser welding - BOP as well as real joining.
It is experienced that it is crucial to have and deploy the flexibility of a fiber bourn laser system, in order to be able to join (thick walled) components successfully by laser welding.
The sensitivity to the weld quality is highly influenced by even small changes to the set-up, why development of welding procedures cannot be based (solely) on bead on plate weld testing.
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STUDY OF THE WELDING CHARACTERISTICS OF A 32 KW DISC-LASER SYSTEM DELIVERING UP TO 2*16 KW IN A COMMON MELT POOL

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¹FORCE Technology, Innovation in Welding Technology, Brøndby, Denmark. ²LWT, Lindoe Welding Technology, Denmark.

Abstract

A new ultra-high power laser system consisting of two 16 kW disc-lasers delivering their power in a common melt pool has been commissioned for industrial applications. The unique setup, comprising a novel mirror-based welding optics and a single fibre delivery system combining beams from the two lasers in a single melt pool, is presented. The initial investigations of the welding characteristics of the combined 32 kW laser system for the welding of thick sheet applications are presented in terms of a study of penetration, width and perceived quality of the autogenously achieved laser beam weld.

Observations on the use of high-power lasers and the choice of proper plasma suppression gas are presented and the effects of using argon and compressed atmospheric air are experimentally compared and verified.

A comparison between penetration and weld width is presented for comparable powers obtained from a single laser source vs. a twin-spot dual-laser source, with each laser providing one half of the same power, to achieve the same sum power-wise. Investigations show a moderate decrease in the obtained weld penetration and an increase in the weld bead width at the top surface.

Keywords: Twin-spot, Disc-laser.

1 Introduction

Recent years development in high-power lasers has shown a trend moving from the powerful, but bulky conventional CO₂-lasers towards smaller and yet more powerful solid-state lasers such as disc- and fibre-lasers, with all the convenience of fibre-guidance, relatively compact form factor and high wall plug efficiencies, while allowing for ever higher outputs. Fibre-lasers are offered at maximum power levels up to 30 kW and beyond from a single source. Typically disc-laser sources are limited to 16 kW from a single source, but recent developments from the manufacturer has made it possible to combine the power of multiple sources via a single fibre into a single common focusing optics, for power delivery into a common melt pool.
In the present experiments two 16 kW (TruDisk 16002) disc-lasers sources from TRUMPF have been combined via a twin-core fibre and a dedicated mirror-based focusing unit. Mirror-based optics are used, as these may be effectively cooled via a heatsink on their rear surface, rather than via radial cooling at the edges of lens-based optics, as is traditionally the case for 1 μm-lasers. Thermal lensing is thus minimized. The mirror is protected by the usual protection glass with an added surveillance-feature, surveying the level of contamination of the glass: At powers of tens of kilowatts even minor contamination of the optical surfaces may prove critical to the welding process or cause back-reflections into the fibre and laser cavity with severe detrimental effects to the optics as a consequence.

2 The twin-laser approach to achieving very high powers

2.1 The 2 x 16 kW setup at LWT

The twin-laser approach to reaching higher powers yields a more versatile tool, as this allows for the use of the two lasers individually or in common. Moreover the common melt pool may be established with tailored power levels from each laser, and its asymmetry may be put into play in order to e.g. suppress spatter or ease the bridging of minor gaps.

![Image of twin-laser setup]

**Figure 1:** The twin-laser setup achieving power levels up to 1x16 kW or 2x16 kW in a common melt pool via a dedicated twin-fiber. Above: The actual setup with the two TruDisk 16002 disc-lasers to the left and the work station with the robot to the right. Below: Illustration of the twin-fiber configuration. (Illustration courtesy of TRUMPF).
The twin laser approach utilizes a tailored twin-fiber, denoted Twin-LLK for transmission of the laser light to the work piece. Furthermore a tailored optical head based on mirror-optics (RFO) rather than traditional lens-based optics is applied in order to mitigate any heat induced deformations from the high power densities achieved on the optical surfaces. The Twin-LLK is effectively made up of two individual 200 µm cores with a 360 µm spacing (center-to-center). Utilizing an RFO with f300 yields a magnification of 1.5:1 and thus the imaging yields 2 x 300 µm spots, separated by 540 µm (center-to-center) as illustrated in Figure 2.

Figure 2: Imaging of the two laser spots of the twin-laser setup via reflective optics, RFO with f300 focal length. Two closely spaced spots are formed. In the present work all tests have been performed with the spot orientation parallel to the welding direction.

With the imaging divided into two spots rather than a single spot, the power density is lower, and consequently the penetration depth for the twin-spot is expected to be lower compared to a single spot at comparable power. However at reasonable process velocities, the melt pool formed by the first spot and the melt pool formed by the second spot will have a common overlap, and the resulting penetration is expected to be improved when compared to having two distinctly separate melt pools. An additional degree of freedom in the welding process is obtained, as the twin spots may be aligned either parallel to the welding seam (for maximum penetration) or perpendicularly to the welding seam (for maximum bridging of gaps).

2.2 Precautions and considerations at very high powers

When applying up to 32 kW to the work piece the usual effects associated with high power laser welding scale and for some, the effects are amplified to a degree where the detrimental consequences to the weld performance or equipment can be severe. Initial trials indicated that the plasma plume may reach heights of 100-150 mm above the work piece if insufficiently suppressed. This in turn causes instability in the weld process due to instabilities in the absorption by the plasma plume and consequently reduces penetration and weld quality.

The plasma plume is therefore suppressed using a novel nozzle design, reducing the effective plume height to 10-30 mm even at 32 kW. The actual plume height is dependant on the choice of process gas, and a difference in plume height of factor of three (roughly estimated by visual observation) is observed when interchanging gasses.

Additional precaution is advised with regards to back-reflections from the process at these high powers. As is well known, back-reflections may be minimized by ensuring a stable keyhole process and by inclination of the beam path, in order to direct the reflections away from the optics via the angle of incidence upon the sample surface. Eruptions from spatter and potential keyhole collapses during welding may temporarily influence the reflections. Any
deposition of spatter, soot or fumes from the process upon the protection glass, protecting the focusing optics, may result in back reflections, potentially causing severe damage to the optical system. The protection glass is therefore equipped with a monitoring system, constantly monitoring the level of pollution on the glass face, and ultimately disallowing further work at high power, until the glass has been cleaned or replaced.

Figure 3: Welding with high powers using twin-spot and co-axial gas nozzle. The plasma plume is clearly visible, although suppressed.

3 Preliminary penetration studies at 16-32 kW powers

3.1 Influence of the process gas: Argon vs. compressed air

Since it is well-known that the choice of process gas influences the welding performance, a comparative study of the effects of argon and compressed atmospheric air on the formation of spatter and on weld penetration was performed. Tests were performed using a single TruDisk laser via mirror-based focusing RFO-optics with a focal length of f300 mm. All tests were performed as bead-on-plate (BOP) in 40 mm S355 structural steel.
Figure 4: Comparative study of the penetration depth with argon vs. compressed air as shielding gas. Test parameters: 16 kW, single-spot, RFO f300, gas-flow: 60 Nl/min (air) and 40 Nl/min (Ar), focal position -3 mm.

Figure 4 indicates a slight increase in penetration at low to moderate process velocities when using argon rather than compressed atmospheric air for plasma suppression. The observed effect is most significant at velocities of 1-3 m/min, achieving more than 15% increase in penetration depth around 1.2 m/min, and becomes negligible at higher speeds.

Table 1: Increase in penetration depth, D, obtained by use of argon compared to air for plasma plume suppression at different process velocities, v.

<table>
<thead>
<tr>
<th>v [m/min]</th>
<th>0.5</th>
<th>1.0</th>
<th>1.2</th>
<th>1.5</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>12</th>
<th>16</th>
</tr>
</thead>
<tbody>
<tr>
<td>ΔD [%]</td>
<td>4.8</td>
<td>11.1</td>
<td>15.6</td>
<td>6.3</td>
<td>13.3</td>
<td>7.1</td>
<td>4.2</td>
<td>0</td>
<td>0</td>
</tr>
</tbody>
</table>

Although the increased penetration depths observed with argon for plasma suppression, the resulting weld quality is impacted by the argon causing instabilities and consequently severe spatter on the weld top side. The increase in spatter follows the trend of the penetration, and is most severe at low velocities.
The observed formation of spatter is clearly audible during welding, where eruptions from the melt pool cause instabilities in the welding process. The observed instabilities are ascribed to the Marangoni Effect: The argon gas reverses the flow of molten material in the melt pool, and causes the melt to move from the weld center upwards and outwards towards the melt pool edge, and in turn causes eruptions.

All subsequent welding tests have been performed using atmospheric air for plasma suppression. Any potential detrimental effects due to oxidation of the work piece by the oxygen contained in atmospheric air are neglected in the present work.

### 3.2 Single spot vs. twin spot: 1x16 kW vs. 2x8 kW

A comparison of the weld penetration depth between a certain power level obtained with a single laser and the same power level obtained using a summation of powers from the two individual lasers has been made. The comparison is made using the same RFO f300 optics, and under comparable boundary conditions.

**Figure 9:** Comparative study of the penetration depth of single-spot 1x16 kW vs. Twin-spot 2x8 kW. The trendline for the twin-spot experiment is an exaggerated approximation. The twin-spots are aligned parallel to the welding direction.
The penetration depth of the twin-spot approach is found to be approximately 12-18% less for the 2x8 kW compared to 1x16 kW at velocities 1.0-1.5 m/min. No tests have been made at higher velocities, but the effect is expected to become more pronounced at higher velocities, when the melt pool of the first spot may be partially solidified when the second spot passes, thus leading to the collapse of the common melt pool and the establishing of two distinct melt pools.

Applying the twin-spot process to reach the same power level as in single-spot obviously impacts the weld penetration as was shown above. But in return the weld width is increased, and this may prove useful in bridging gaps and compensating for misalignment relative to the weld seam when welding thick sections.

*Figure 10:* Weld profiles obtained using a total laser power of 16 kW. Above: Single-spot, 1x16 kW. Below: Twin-spot, 2x8 kW. Configuration (both): RFO 300, focal pos. -3 mm in S355, 40 mm sheet, BOP.
The weld width at the top surface is increased 45-65% in the 2x8 kW welds compared to the 1x16 kW welds at the same process speeds in the range 0.8-1.5 m/min.

An additional increase in the welding width can be attained by aligning the twin-spots perpendicularly to the welding seam. This approach has not yet been attempted with the LWT setup, but is considered a topic for further studies, particularly in conjunction with cold-wire application, where the widening of the melt pool may assist in stabilising any wobbling of the wire.

### 3.3 Weld penetration at powers exceeding 16 kW

Utilizing disc-lasers at power levels exceeding 16 kW is a novelty outside the academia, and is expected to present an attractive means of speeding up heavy section welding, especially in hybrid configurations with traditional arc-welding methods. Autogenous penetration profiles at various process speeds are nevertheless interesting for initial design of the root welding pass parameters.

A series of BOP welding tests at 24 kW (2x12) and 32 kW (2x16) was devised. The results are shown in Figure 12, from which it is obvious that, for low to moderate process speeds of 1.0-1.5 m/min, the penetration obtained with twin-spot at 2x12 kW is comparable to the that of the single-spot 1x16 kW (Approximately 3% higher for 2x12 kW).

At low speeds in the range 1-3 m/min, the penetration depth at 32 kW (2x16) in the velocity range 1-3 m/min is found to increase by 25-35% vs. the penetration obtained using only one laser at 16 kW, single-spot. At moderate to high speeds the penetration depth for the 32 kW increases by 45% to 128% at the very high speeds. This observation is ascribed to the formation of a common melt pool and the first, leading spot pre-heating the material to increase the penetration of the second, trailing laser spot.
Figure 12: Comparative study of the penetration depth of single-spot 1x16 kW vs. Twin-spot 24 kW (2x12) and 32 kW (2x16 kW). Twin-spots aligned parallel to the welding direction.

Figure 13: Percentual increase in penetration depth at 32 kW (2x16) vs. 1x16 kW.

4 Conclusions

The preliminary welding characteristics of a novel twin-spot, dual-laser capable of delivering up to 32 kW through a single optical system to a common melt pool have been studied, and the preliminary BOP welding test results are presented.

The influence of the plasma gas is investigated, and a comparison of argon vs. atmospheric air shows a slight increase in penetration at low to moderate process velocities when using argon for plasma suppression. The observed effect is most significant at velocities of 1-3 m/min, achieving more than 15% increase in penetration depth around 1.2 m/min, and
becomes negligible at higher speeds. The increased penetration depths observed with argon are countered by a severe impact on the weld quality, introducing instabilities and consequently severe spatter on the weld top side. The increase in spatter follows the trend of the penetration, and is most severe at low velocities.

A comparison of welds performed at comparable powers but using either 1x16 kW or 2x8 kW is presented, and the penetration depth of the twin-spot approach is found to be approximately 12-18\% less for the 2x8 kW compared to 1x16 kW at velocities 1.0-1.5 m/min. The weld width at the top surface is increased 45-65\% in the 2x8 kW weld beads compared to the 1x16 kW welds at the same process speeds in the range 0.8-1.5 m/min.

A series of tests at 24 kW (2x12) and 32 kW (2x16) show that, for low to moderate process speeds of 1.0-1.5 m/min, the penetration obtained with twin-spot at 2x12 kW is comparable to that of the single-spot 1x16 kW.

At low speeds in the range 1-3 m/min, the penetration depth at 32 kW (2x16) in the velocity range 1-3 m/min is found to increase by 25-35\% vs. the penetration obtained using only one laser at 16 kW, single-spot. At moderate to high speeds the penetration depth for the 32 kW increases by 45\% to 128\% at the very high speeds.

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