Thermo-Mechanical Model Development and
Experimental Validation for Directed Energy Deposition
Additive Manufacturing Processes

A Dissertation in
Mechanical Engineering
by
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Abstract

Additive manufacturing (AM) enables parts to be built through the layer-by-layer addition of molten metal. In directed energy deposition (DED) AM, metal powder or wire is added into a melt pool that follows a pattern to fill in the cross section of the part. When compared to traditional manufacturing processes, AM has many advantages such as the ability to make internal features and to repair high-value parts. However, the large thermal gradients generated by AM result in plastic deformation. Thermo-mechanical models must be developed to predict the temperature and distortion produced by this process.

Thermo-mechanical models have been developed for AM by several investigators. These models are often validated by measuring the temperatures during the deposition of a small part and the final distortion of the part. Unfortunately this is not a sufficient validation method for the non-linear thermo-mechanical model. Although good agreement between the thermal model and the temperatures measured during a small depositions can be achieved, it does not necessarily mean that the model will be accurate for an industrially relevant part that requires $10^2 - 10^4$ tracks and hours of processing time. The relatively small deviations between the model and the validation will propagate when modeling large depositions and could produce inaccurate results. The errors in a large part will be increased further if the assumptions made of the thermal boundary conditions are not appropriate for the system.
The objective of this work is to develop and experimentally validate thermo-mechanical models for DED. Experiments are performed to characterize the distortion induced by laser cladding. The depositions require many tracks and nearly an hour of processing time, during which the temperature and the deflection are measured in situ so that the response of the plate to each deposition track is understood. Measurements are then made of the convection caused by two different laser deposition heads. Thermo-mechanical models are developed by implementing the measured rate of convective heat transfer and the temperature dependent material properties. The models are validated using in situ measurements of the temperature and the deflection generated during the process, as well as post-process measurements of the residual stress and the distorted shape. Finally, experiments and models are used to investigate the impact of feedstock selection, either powder or wire, on the DED process.
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Chapter 1

Introduction

Additive manufacturing (AM) enables parts to be built through the layer-by-layer addition of material. In directed energy deposition (DED) AM, metal powder or wire is added into a melt pool that follows a pattern to fill in the cross section of the part, layer by layer. A laser is often used to create the melt pool into which the material is injected, as shown in Figure 1.1. DED has several advantages over traditional manufacturing processes. For example, it can produce parts with internal features or graded materials. In addition, existing parts can be clad to replace material that has been lost through wear or damage. This is attractive because repairing a high value component can extend its operational life, saving cost compared to manufacturing a replacement. Despite these advantages over traditional manufacturing processes, AM has yet to reach its potential. The National Institute of Standards and Technology (NIST) estimates that currently AM accounts for only 0.01% to 0.05% of the components manufactured in the major manufacturing sectors [1]. NIST projects that as the cost decreases, AM has the potential to significantly disrupt the current manufacturing climate. However, before widespread adoption of AM can occur, the deformation of the part, caused by the large thermal gradients and the contraction of the molten material, must be understood so that it can be controlled.
1.1 Prior work

1.1.1 Welding

Welding distortion, which is a similar problem as AM distortion, has been extensively investigated. Therefore, it is wise to first consider the work done to understand and control welding distortion. The large thermal gradients generated during the welding process lead to residual stresses and complex distortion. Masubuchi classified six modes of welding distortion, three of which are out of plane and three that are in plane [2]. The distortion from these modes can be detrimental to the performance of welded structures. Therefore, welding distortion has been studied for the last four decades [3–5].

Research on weld modeling has evolved over time, producing models with increased accuracy. Early on, only the material around the weld was included in the model because the volume of material affected by the weld is small compared to the
rest of the structure [6]. In order to better predict distortion, Brown and Song coupled their non linear thermomechanical weld model to an elastic model of the remaining structure [7]. The coupled elastic structure provided resistance to the thermal elastic distortion, which could not be fully accounted for when only the small model was used. Goldak et al. developed a double ellipsoid heat source model, in which the moving heat source is distributed throughout a volume of the weld [8]. This model was shown to produce more accurate results than the existing models of a surface heat source. The importance of phase change was illustrated by Papazoglou and Masubuchi when modeling the welding of steel plates [9]. Validations of their simulation showed that the best results were made when including the phase transformation in the model.

Accurate weld models have been used to design and optimize processes and to develop distortion mitigation techniques. Michaleris et al. developed a thermoelasticplastic model and used it to optimize a weldment according to the manufacturing process and the service life of the structure [10]. Their model was also used to predict welding induced distortion [11]. A distortion mitigation technique using thermal transient tensioning was then investigated and optimized to minimize welding induced buckling [12, 13]. Jung et al. also investigated the ability of thermal transient tensioning to mitigate buckling [14]. Their analysis was performed with thermal tensioning and different welding sequences. They found that the buckling distortion was affected by the chosen welding sequence.

When large structures are to be welded together, multipass welding is required. The additional weld beads make it a more complex problem. Shim et al. modeled the multi-pass weld process by treating each pass as a lumped mass [15]. They found
more complex thermal cycles resulting from the sequential deposition of weld beads. Hong et al. investigated the possibility of lumping multiple passes together, as opposed to considering each weld bead independently [16]. They found that lumping multiple passes together introduced more heat into the part than when each pass was considered individually. The lumped treatment of multiple passes led to underestimating the transverse residual stresses while having little effect on the longitudinal stresses.

In summary, improved process understanding has allowed more detailed models to be developed that more accurately capture the physics of the welding process. As a result, these models have been used to design techniques to mitigate distortion and produce higher quality structures. Therefore, it would be advantageous to use a similar approach for AM.

1.1.2 Additive Manufacturing

Although AM is similar to welding, it requires additional considerations when modeling. The material is deposited using a large number of tracks, which require a lot of time. Consequently, errors will propagate when modeling such a long process. Therefore, a greater effort is required to improve the model accuracy and to thoroughly validate the model. Large depositions are necessary to validate the models because of the greater amount of heat input into the part and the longer processing time. As the temperature of the part increases over time, the heat transferred from the newly deposited material into the part decreases while the heat loss into the surroundings increases. The rate of heat conduction away from the deposited material becomes even less as tall slender structures are built. The heat loss due to convection is increased even
more by the inert gas jets that are used to shield the molten material, to protect the laser optics, and to deliver powder to the melt pool. Therefore, large depositions must be used to ensure that the evolving energy balance is adequately modeled.

1.1.2.1 Temperature modeling and measurement

Understanding the thermal history of a deposited part is of great importance because it influences the microstructure, material properties, residual stress, and distortion. Therefore it is advantageous to perform measurements which can be used to gain insight into the process and to develop and validate accurate thermal models.

Measurement of temperature

The temperature history of a deposition can be measured using a variety of techniques. Thermocouples are often welded to the substrate before processing to measure its thermal history [17–22]. However, as more layers are added, the thermocouples are further removed from the deposition. To address this issue, Ensz et al. paused their deposition so that additional thermocouples could be attached on the wall to measure the temperature near the deposition when it was resumed [23]. Non-contact thermal measurements have also been used to measure the temperature of the evolving surfaces. For instance, infrared cameras and pyrometers have been used to measure the temperature distribution on the deposited surface [24,25] and to measure the melt pool temperature and its size [21,26–30]. However, these methods can only measure the surface temperatures and cannot be used to study the internal temperatures. Instead, these measurement techniques can be used to validate thermal models which can be used for in-depth investigations of the temperature history.
Thermal models

Although numerous studies have developed thermal models for DED processes, there appears to be no uniform approach for implementing the thermal boundary conditions. For example, Wang and Felicelli performed a parametric study to assess the significance of conduction, convection, emissivity, latent heat, and the heat source on a two-dimensional thermal model of a thin wall deposition [31]. It was calculated that 9.1% of the heat was dissipated through convection and radiation and therefore changes in these boundary conditions had little impact on the simulated temperature profile around the melt pool. Considering this, convection has been neglected in some models [32–35]. In contrast, Michaleris found that neglecting convection and radiation on the exposed element surfaces in a three-dimensional model would lead to errors [36]. Many studies have included the effects of convection, but assumed it to be uniform over the surface. The heat transfer coefficient for natural convection, which is approximately 10 W/m²/K, is often used [21, 27, 37–43, 43].

The thermal boundary in laser based AM is complex and it may not be accurate to assume uniform convection. An inert gas jet is often used to protect the laser optics and to shield the molten material from oxidizing. Additionally, when powder is used as the deposition material, it is often delivered using an inert gas jet. However, it is well known that gas jets create localized forced convection [44–63], and therefore more complex convection models should be used to accurately model the heat transfer.

Some studies modeling DED have considered localized forced convection. Occasionally a larger coefficient of convection is applied uniformly to all free surfaces in order to approximate the greater heat transfer caused by the forced convection
However, this does not capture the complex distribution of the forced convection generated by gas jets. Other studies have attempted to incorporate more accurate models of forced convection. Ghosh and Choi used an empirical equation defined by another research group [66]. However, the empirical model from the other study may not be accurate because forced convection is dependent on the geometry and orientation of the nozzle, the interactions of multiple flows, and the inclusion of particles in the jet. Zekovic et al. used computational fluid dynamics (CFD) to calculate the convection acting on the deposited geometry [67]. Unfortunately, this makes the accuracy of the DED model dependent upon the accuracy of the CFD model, which requires many assumptions.

1.1.2.2 Microstructure and residual stress

Characterization of the deposition microstructure

The microstructure that is developed from the deposition has been of interest to many researchers. For instance, experiments have shown that grain growth depends on processes parameters that influence the temperature gradients, such as laser power and travel speed [32, 68, 69]. In fact, grain growth has been shown to follow the path of the laser and fine grains have been observed that are similar to the grains developed by rapid cooling processes [70]. Consequently, simulations of the thermal cycles and cooling rates are used to study the microstructure evolution of materials deposited using DED [30, 38, 39, 43, 69, 71, 72].

The microstructure has been shown to be influenced by the thermal cycles resulting from multi-layer depositions. In 2004, Kelly and Kampe observed layer-band and gradient morphologies in deposited Ti-6Al-4V [73]. They found that the last three
deposited layers did not show the same microstructure as the previously deposited layers. Their simulations revealed that the thermal cycles in these layers reached sufficient temperatures to induce microstructural changes \[38\]. Zheng et al. found that the rapid quenching effect as the molten material cools decreases and eventually disappears as more layers are deposited \[29, 39\]. In contrast, Ensz et al. found that the strength of deposited material did not decrease as successive layers were deposited and the temperature exceeded the annealing temperature \[23\]. This was attributed to the brief time that the material exceed the annealing temperature. The simulations performed by Xiong et al. have shown that in a multi-layer deposition of tungsten carbide-cobalt the prior layer experiences temperatures that exceed the eutectic temperature \[30\].

**Residual stress modeling and measurement**

The deposition process creates residual stresses that have been measured \[17, 22, 23, 41, 42, 74, 75\] and studied using simulations \[18, 27, 37, 42, 67, 76–80\]. It has been found that residual stress is affected by a variety of factors, including the process parameters, deposited structure, layer count, and preheating. Ensz et al. found that slower speeds and lower laser power generated greater stresses than compared to using high power and speed \[23\]. Process maps have been developed to select parameters to minimize the generation of residual stresses \[76\]. However, each process map is specific for the processing method, material, and geometry used to create the map. In addition to the processing parameters, the deposition strategy and geometry has also been shown to affect the temperature history and resulting residual stresses \[23, 27, 67, 75\]. While some studies have shown that residual stresses are affected by the raster pattern \[17, 74\],
Rangaswamy et al. found that the deposition patterns they investigated had no effect on the residual stress [75].

Accurate prediction of stress has been shown to depend on the inclusion of phase transformations in the model. Ghosh and Choi demonstrated the importance of including phase transformation effects in the model by comparing the residual stresses predicted with and without phase consideration [81]. Denlinger et al. found it necessary to include the effect of phase transformations on strain in their model to accurately predict the stresses generated by the electron beam deposition of a Ti-6Al-4V wall [22].

Post-process heat treatments

Many of these observed microstructure phenomena and residual stresses are susceptible to post process heat treating. Griffith et al. commented that layer effects induced during laser engineered net shaping (LENS) are typically removed using post process heat treatments [68]. Dinda et al. found that annealing Inconel® 625 removed the fine dendrites that developed during the deposition [82]. Grum et al. found that heat treating could reduce the microhardness of the deposited material [17]. Baufeld et al. found that the mechanical properties of deposited materials varied greatly depending on the post process heat treatment [83, 84]. However, these heat treatments cannot solve the problem of distortion or crack and pore formation that occur during DED [85, 86]. The deposition process must be understood and controlled to avoid effects that cannot be removed using post process heat treatments.
1.1.2.3 Distortion modeling and measurement

Because the distortion resulting from DED cannot be easily removed though post process heat treatments, thorough understanding of the distortion must be obtained so that it can be mitigated. Fortunately, experimentally validated models can be used to study the distortion induced by DED in conjunction with experiments. However, it is necessary to thoroughly validate these models considering the non-linear material properties and complex boundary conditions of the DED process. Validations are typically performed using post process or in situ measurements of the deposition.

Post process measurement of the final distortion

Post process measurements of distortion can provide valuable insight into the accumulation of distortion and reference data for model validation. The final shape of the substrate and deposition can be compared to the final shape predicted by the model. For example, Klingbeil et al. used experiments and models to investigate the warping that resulted from depositing stainless steel wire into a melt pool created by an arc [87]. The material was deposited using different patterns and preheating strategies. The final distortion was measured and used to validate simple models. It was found experimentally that pattern selection and substrate preheating could reduce the distortion. However, their model assumed elastic, perfectly plastic material properties and was unable to capture the effect of deposition pattern on distortion.

Chiumenti et al. used post process distortion and stress measurements and in situ temperature measurements to validate their model [20]. Inconel\textsuperscript{718} was deposited in
10 layers to build a long wall. The temperature and final distortion in the longitudinal and transverse axes agreed well with the measured values.

Lee et al. developed a thermomechanical model to predict distortion, temperature and stress in an aerospace component [88]. They deposited a simple wall to qualitatively compare the measured distortion with the distortion predicted for their complex component. Unfortunately this comparison only confirmed that the deposition caused the substrate to bow.

Anca et al. performed computational modeling of a tungsten inert gas deposition onto a plate and validated their results with measurements of the post process distortion of the plate [89]. The liquid to solid phase transformation of Ti-6Al-4V was used in the model, but not the alpha-beta phase transformation. A four layer, single track wide wall was deposited and modeled. The model predicted the bowing distortion in the middle of the plate to be 0.7 mm, whereas the actual distortion was measured to be 0.5 mm.

Marimuthu et al. modeled the thermal distortion of a substrate during the laser deposition of an Inconel® 718 turbine component [90]. The part was a cylinder deposited onto a curved substrate. Various fill patterns were investigated to minimize distortion. Post process measurements of the deposition revealed the part deformed less than the model predicted (2.1 mm vs. 5.9 mm). This discrepancy was attributed to internal stresses in the plate prior to the deposition, which were not incorporated in the model. Even though the predicted distortion magnitude was incorrect, the model was used qualitatively to find a deposition pattern that resulted in the least amount of distortion.

Although the final measured distortion has been used to validate models, it does not provide any information on how the distortion accumulates. A more thorough
measure of the distortion is obtained by using in situ measurement techniques, which provide insight into the distortion history resulting from each deposition pass.

**In situ measurement of distortion**

Plati and coworkers measured the in situ deflection of the end of a substrate onto which a single track was deposited [18]. The measurement was used to validate their thermo-mechanical model. A metal matrix composite was deposited in a single track onto a stainless steel substrate. The temperature was measured with two thermocouples on the bottom surface and the deflection was measured with a linear variable displacement transducer (LVDT). The predicted temperature and deflection matched well with the in-situ measurements.

Three-dimensional image correlation techniques were used by Ocelik and coworkers to measure the strain and distortion on the bottom of a plate onto which single and multiple tracks were clad [91]. The measurements revealed that the displacement primarily occurs during the deposition. Longitudinal bowing and transverse angular distortion were measured from a single deposition track. In addition to the single track, another test was performed that deposited 10 tracks onto the surface of a substrate. The strain measured at the center point increased sharply with each deposition, then relaxed between tracks. Furthermore, the strain increase resulting from each track decayed to the point where no additional strain accumulated after the deposition of the sixth track.

Grum and Žnidarič used high temperature rosettes to measure the strain from the beginning to the end of a laser clad process [17]. Various raster patterns were investigated. The strain gages provided in situ measurement of the strain and curvature on the bottom of the substrate. The measurements were performed on two axes and
revealed that a raster pattern with alternating deposition tracks produced the least amount of curvature.

Lundbäck and Lindgren developed a model for metal deposition that incorporated dislocation density in the flow model [21]. In-situ temperature and deformation measurements were performed for validation. A pyrometer and thermocouples were used to measure the temperature and an optical measurement system was used to measure the distortion. Single track wide, 10 layer tall walls were deposited to calibrate the model. A complex multiple-path deposition consisting of 10 layers was used to validate their model. The distortion was measured at several location on the substrate and was found to agree well with the predicted results.

Denlinger et al. used in situ deflection measurements to identify the need to include the effects of phase transformations in their DED model [22]. A 16 layer, single track wide Ti-6Al-4V wall was deposited using an electron beam and modeled. The substrate was cantilevered and the deflection at the end was measured using a laser displacement sensor (LDS). The deflection history showed that after a few deposition layers, there was no net increase in deflection. When the phase transformation was not accounted for, the model predicted a nearly uniform increase in deflection with each layer that did not match the measured in situ deflection. Good agreement was achieved between the model and the measurements when the effects of the alpha-beta phase transformation were accounted for in the temperature dependent mechanical properties. Their model was further validated by modeling a very large industrial part [92]. However, because the electron beam deposition is performed in a vacuum, convection was excluded from their model.
1.2 The Need

Accurate thermo-mechanical models of DED can only be developed when the energy balance of the entire process is fully understood. Although the literature presents numerous thermo-mechanical models, many of these are developed with assumptions that may or may not be appropriate. Furthermore, they are often validated with small depositions that do not allow all aspects of the energy balance to be captured. Therefore, model validation must be performed using in situ and post process measurements of large depositions that allow all aspects of the energy balance to be observed.

1.3 The Objective of This Research

The objective of this work is to develop and experimentally validate thermo-mechanical models for directed energy deposition. These models will predict the temperature and distortion history as well as the residual stresses in the part. Experimentally measured surface convection will be used in addition to radiation from the literature to define the thermal model boundary conditions. Temperature dependent thermal and mechanical material properties will be used in the model. Measurements of large depositions using $10^1$-$10^2$ tracks will be used to validate the model. In situ measurements of the temperature and deflection in addition to post process distortion and residual stress measurements will be compared with the model predictions. Finally, these models will be used to investigate the sensitivity of the process to changes in the inert gas flow rates, which affect the forced convection, and the impact of feedstock selection on the temperature and distortion history.
1.4 Thesis Outline and Significant Contributions

The following is an outline of the work performed to develop and validate thermo-mechanical models for directed energy deposition additive manufacturing. The significant findings from each chapter listed.

- Chapter 2: *In-situ* monitoring and characterization of distortion during laser cladding of Inconel® 625 plates
  - Significant contributions
    1. Linear heat input, a common metric used to predict distortion, is shown to be inadequate for laser cladding.
    2. A new metric is proposed to relate laser power, travel speed, and hatch spacing to distortion.
    3. Each out-of-plane distortion mode responds differently to multiple clad layers.
    4. The long processing times allow heat transfer through convection and radiation to become significant.

- Chapter 3: Measurement of forced surface convection in directed energy deposition additive manufacturing
  - Significant contributions
    1. A method is developed to acquire the convection from the energy balance during hot-film constant voltage anemometry.
2. The method is used to characterize the convection generated by a commercial deposition head.

3. For the deposition head investigated, nozzle configuration as well as the surface roughness and configuration are shown to significantly impact the forced convection.

- Chapter 4: Thermo-mechanical model development and validation of directed energy deposition additive manufacturing

  1. The results demonstrate that measurement based forced convection should be incorporated into thermal models of DED.

  2. Neglecting forced convection is shown to not only increase the process temperatures significantly, but also the distortion and residual stresses.

- Chapter 5: The impact of argon shielding flow rate on laser engineered net shaping (LENS) of Ti-6Al-4V

  1. Decreasing the flow rate reduces the convection, resulting in lower temperatures, distortion, and residual stresses.

  2. The inert gas flow rate must be well controlled to ensure consistent processing.

- Chapter 6: Selection of powder or wire feedstock material for the laser cladding of Inconel® 625
- Significant contributions

1. Cladding with powder generates higher temperatures than wire cladding because a greater percentage of laser energy is absorbed by the part.

2. The amount of energy absorbed by the substrate is directly related to the amount it distorts.

3. Powder feedstock is preferable to wire because it requires fewer layers, which result in less total distortion, to achieve a desired clad thickness.
Chapter 2

In situ monitoring and characterization of distortion during laser cladding of Inconel® 625

2.1 Abstract

Laser cladding is a low heat input metal deposition process that provides lower dilution and distortion levels than comparable arc welding based processes. Even though distortion of the substrate material is significantly reduced in laser cladding, the high temperatures and rapid thermal cycles produced by the laser heat source still result in measurable distortion. This distortion is particularly concerning in repair or refurbishment applications where dimensional control is critical. The impact of multi-layer laser cladding on the amount and mode of distortion and thermal history at selected substrate locations in Inconel® 625 plates is investigated and quantified for different processing conditions and deposition patterns. In situ measurements of the longitudinal bowing and transverse angular distortion modes are made using a laser displacement sensor followed by post-process measurements. The results show that each distortion mode occurs simultaneously, causing the plate to twist in response to the deposition pattern. The selection of processing parameters also had several unexpected impacts on the magnitude and mode of distortions as well as the temperatures measured on the substrate. For example, similar substrate temperatures are observed for all

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processing conditions, regardless of the magnitude of the linear heat input or the length of the processing time. The addition of a second layer when using a transverse hatch pattern also shows that the transverse angular distortion mode can be minimized, unlike the case of longitudinal bowing with a longitudinal hatch pattern, which demonstrates a cumulative increase in longitudinal bowing with an additional layer. In order to account for these unexpected observations, a new metric is proposed to replace linear heat input for cladding. This new metric, clad heat, takes into account the laser power, travel speed, and hatch spacing and is shown to better predict the magnitude of cladding distortion.

2.2 Introduction

Laser cladding is a low heat input metal deposition process in which a powder or wire feedstock material is melted and deposited on a metallic substrate. In this process, thin metal layers are deposited and fused to a metallic component in order to increase operational lifetime by adding a corrosion or wear resistant material to improve its properties and performance or to replace material that has been worn away [93]. Multiple layers can be applied in order to achieve the required deposition thickness and even to add new features to enhance the functionality of the component. During the deposition of each layer, a molten pool consisting of both the melted feedstock and a small portion of the substrate material is formed using the laser. This molten material then rapidly solidifies and cools as the laser traverses across the part, generating large thermal gradients in the part that can lead to the formation of large levels of residual stress and distortion.
The part distortion experienced during cladding can be very similar to that experienced during welding processes. Welding-induced distortion is complex, since it results from three in-plane and three out-of-plane distortion modes, as classified by [2]. Because of this complexity, welding distortion and residual stress formation have been extensively investigated using finite element modeling and experimental investigations [3]. The cladding process is similar to welding but differs due to the large number of passes, the volume of deposited material, and the lack of a distinct joint geometry. To better understand the cladding process, researchers have investigated how the process affects the final distortion of the part to understand the mechanical response resulting from changing the deposition pass orientation [20], sequencing of the deposition passes [74], preheating the part [87], fixturing the part [94], and validating model predictions [89]. These studies have been performed using a variety of materials and processing conditions. However, they have only considered the final distortion of the workpiece and not how the distortion accumulated in the part during the cladding process.

Other studies have used in situ techniques to measure the strain and/or displacement from each pass during additive manufacturing processes. [17] mounted high temperature strain gages on the bottom of a steel substrate to measure strain evolution while laser cladding several patterns on the top surface of the part using a variety of laser energy densities. [18] validated their model by using a linear variable displacement transducer to measure the free end deflection of a narrow stainless steel substrate clad with a metal matrix composite powder. [91] performed in situ strain and deformation measurements using digital image correlation during single and multi-pass
laser depositions of several powder materials on steel and stainless steel plates. [21] measured the center point distortion while depositing a complex path onto a substrate using a GTA welding based process to validate their model. Even though these previous efforts have provided information on the magnitude of *in situ* distortion measurements, none has provided insight into the different modes of distortion during the cladding process.

Out-of-plane distortion is a result of two modes. The longitudinal bowing distortion mode is schematically shown in Figure 2.1(a) as the plate bends along the same axis as the deposition pass, while the transverse angular distortion mode causes an angular bend in the plate, as shown in Figure 2.1(b). The objective of this work is to gain an understanding into how out-of-plane distortion accumulates during a multi-layer cladding process by capturing both the magnitude of distortion accumulation as well as the mechanisms governing it. *In situ* deflection measurements are made at a point on the bottom of the free end of a cantilevered plate to acquire the mechanical response caused by the deposition of individual laser cladding passes. These *in situ* distortion measurements are then correlated with temperature measurements made at selected locations on the substrate. The final distorted shape of the plate is also measured to capture the effects of both modes acting simultaneously. Multiple laser power and travel speed combinations, as well as different deposition patterns are used to provide insight into how the distortion modes resulting from laser cladding compare to those from welding processes. The correlation of the linear heat input metric to the distortion is investigated, and the impact of the large volume of clad material and the effect of the large number of passes required to clad an area using multiple clad layers are considered.
2.3 Experimental Procedure

2.3.1 Cladding Process/Conditions

A series of powder-fed multi-layer Inconel\textsuperscript{R} 625 clads are fabricated on Inconel\textsuperscript{R} 625 plates which are 152.4 mm long, 76.2 mm wide, and 6.35 mm thick. Each clad is fabricated using a YLR-12000 IPG Photonics\textsuperscript{R} fiber laser, which operates in the 1070 to 1080 nm wavelength range. The laser is delivered to the part through a 200 μm diameter fiber, which is then passed through a 200 mm focal length collimator and 200 mm focal length optics. Inconel\textsuperscript{R} 625 powder with a particle size distribution between 44 and 149 μm (-100/+325 sieve size) is injected into the melt pool coaxially through a Precitec\textsuperscript{R} YC50 deposition head, which is nominally positioned 10 mm above the part surface. At this location, a beam diameter of approximately 4 mm is obtained, which is measured using a Primes Beam Focus Monitor. Two coaxial argon jets are used to shield the optics and the melt pool, preventing atmospheric contamination in the clad, as well as to deliver the powder to the melt pool. Separate gas flows with a flow rate of 9 L/min are used for each jet.
The impact of changes in deposition patterns, laser power, and travel speed on the accumulation and final distortion of the substrate plate is investigated. Table 2.1 provides a summary of these different process conditions. The powder mass feed rate and the hatch spacing were predetermined for each laser power, travel speed, and mass feed rate combination. Hatch spacing is the distance between laser centers of successive passes. It is directly related to the laser beam size and is determined so that a 40% to 60% overlap with the previous pass is achieved. The average layer thickness is calculated from the maximum height of the deposition.

Table 2.1. The test cases and process conditions used.

<table>
<thead>
<tr>
<th>Case number</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
</tr>
</thead>
<tbody>
<tr>
<td>Deposition pattern</td>
<td>Longitudinal</td>
<td>Transverse</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Nominal laser power (kW)</td>
<td>1.0</td>
<td>1.0</td>
<td>2.5</td>
<td>1.0</td>
<td>1.0</td>
<td>2.5</td>
</tr>
<tr>
<td>Travel speed (mm/s)</td>
<td>10.6</td>
<td>4.2</td>
<td>10.6</td>
<td>10.6</td>
<td>4.2</td>
<td>10.6</td>
</tr>
<tr>
<td>Mass feed rate (g/min)</td>
<td>7.9</td>
<td>7.9</td>
<td>19.0</td>
<td>7.9</td>
<td>7.9</td>
<td>19.0</td>
</tr>
<tr>
<td>Pass length (mm)</td>
<td>132</td>
<td>135</td>
<td>127</td>
<td>72</td>
<td>72</td>
<td>71</td>
</tr>
<tr>
<td>Hatch spacing (mm)</td>
<td>1.14</td>
<td>1.27</td>
<td>2.03</td>
<td>1.14</td>
<td>1.27</td>
<td>2.03</td>
</tr>
<tr>
<td>Number of passes per layer</td>
<td>56</td>
<td>57</td>
<td>33</td>
<td>115</td>
<td>104</td>
<td>65</td>
</tr>
<tr>
<td>Time to complete 1 layer (min)</td>
<td>27.4</td>
<td>45.9</td>
<td>15.4</td>
<td>36.7</td>
<td>50.1</td>
<td>20.5</td>
</tr>
<tr>
<td>Average layer thickness (mm)</td>
<td>0.5</td>
<td>1.2</td>
<td>1.1</td>
<td>0.5</td>
<td>1.1</td>
<td>1.0</td>
</tr>
<tr>
<td>Powder capture efficiency (%)</td>
<td>38</td>
<td>41</td>
<td>62</td>
<td>38</td>
<td>37</td>
<td>57</td>
</tr>
<tr>
<td>Linear heat input (J/mm)</td>
<td>94</td>
<td>236</td>
<td>236</td>
<td>94</td>
<td>236</td>
<td>236</td>
</tr>
<tr>
<td>Heat input per layer (kJ)</td>
<td>695</td>
<td>1811</td>
<td>989</td>
<td>782</td>
<td>1777</td>
<td>1091</td>
</tr>
<tr>
<td>Clad heat (J)</td>
<td>108</td>
<td>302</td>
<td>479</td>
<td>108</td>
<td>302</td>
<td>479</td>
</tr>
</tbody>
</table>

The process parameters are inter-related, prohibiting a study in which a single parameter is varied at a time. For example, holding laser power and mass feed rate constant while decreasing the travel speed from 10.6 mm/s in Case 1 to 4.2 mm/s in
Case 2 affects the width and thickness of the deposited pass. The wider and thicker passes in Case 2 are a result of the increased linear heat input, which is a function of laser power and travel speed.

The nature of the powder fed process results in only a percentage of the powder being added to the melt pool. The powder capture efficiency, which is the percentage of powder that is added to the melt pool, is calculated using:

\[
\text{powder capture efficiency} = \frac{m_f - m_i}{fLpL} \tag{2.1}
\]

where \( m_i \) is the initial weight of the plate before cladding, \( m_f \) is the final weight of the plate after both layers are clad, \( f \) is the powder feed rate, \( l \) is the length of each pass, \( p \) is the number of passes per layer, \( L \) is the total number of layers, and \( v \) is the travel speed of the laser. Linear heat input is a measure of the energy density of each pass and is a commonly used metric in welding. It is calculated according to:

\[
\text{linear heat input} = \frac{P}{v} \tag{2.2}
\]

where \( P \) is the laser power. Clad heat is a new metric that is proposed in this study and will be discussed in Section 2.5.2.

Figure 2.2 shows the clad area and deposition paths investigated. No post-process treatment was performed on the clad surface. The deposition path vectors in each pattern are all in the same direction. A second layer is deposited on each plate by repeating the same process conditions as the first layer. An approximate 15 min delay was utilized
between each layer to allow the surface layer to be inspected and the plate to reach a near-uniform temperature, which was measured with thermocouples to be approximately 100 °C, before the deposition of the second layer.

2.3.2 *In situ* Deflection and Temperature Measurement Method

In order to make *in situ* measurements of the distortion during the cladding process, each plate is mounted and clamped on one end, allowing the opposite end to distort freely during the deposition using the experimental setup shown in Figure 2.3. *In situ* distortion measurements are taken with a Keyance LK-031 Laser Displacement Sensor (LDS) aimed near the edge of the bottom of the free end of the substrate and a Keyance LK-2001 controller. This LDS has a range of 10 mm, a target diameter of 10 μm and a resolution and accuracy of 1 μm. The controller analog output voltage signal, which indicates the measured distance between the LDS and the bottom of the plate, is acquired using a National Instruments NI 9205 module and recorded in a Labview® environment at a rate of 20 Hz. The signal is then filtered using a simple moving average. A thermocouple is mounted to the LDS to monitor its temperature and to verify that it does not exceed its maximum allowable operating temperature (50 °C). A high-temperature bag is placed over all of the electronics, excluding the LDS, to protect them from metal powder during the deposition.

Although both modes of distortion simultaneously occur, the measurement setup and two different deposition patterns allow the deflection from each mode to be measured independently. For example, deflection resulting from the longitudinal pattern in Cases 1 through 3 is dominated by the longitudinal bowing mode (Figure 2.4(a)). Similarly, the
Fig. 2.2. Images of the clad parts showing the deposition paths and the thermocouple locations for (a) the longitudinal and (b) the transverse deposition patterns.
Fig. 2.3. The complete experiment setup without the high temperature bag protecting electronics.
deflection measured during the deposition of the transverse pattern in Cases 4 through 6 is dominated by the transverse angular mode (Figure 2.4(b)). An additional benefit of this scheme is the ability to measure the deflection at varying distances from the deposition. Each pattern allows the relative position between the fixed LDS measurement location and the deposited track to vary. This will be discussed in greater detail in Section 2.4.1.1.

Fig. 2.4. Illustrations showing that the LDS measures the deflection resulting from a) longitudinal bowing mode of distortion in Cases 1-3 and b) transverse angular mode of distortion in Cases 4-6.

Temperatures are measured at various locations on the top and bottom of the substrate plate using Omega GG-K-30 type K thermocouples, which have a measurement uncertainty of 2.2°C or 0.75%, whichever is larger. The thermocouple signals are acquired using a National Instruments NI 9213 module. The schematic diagrams in Figure 2.5 show the locations of the four thermocouples from the bottom and top views of the substrate plate, in addition to the location of the LDS measurement. Two thermocouples are attached to the bottom of the substrate, with one (TC 1) located at the center point
of the bottom surface and the other (TC 2) located approximately 4 mm from the edge of the plate and half way along the length of the plate. Two additional thermocouples are placed on the sidewalls of the substrate, with one (TC 3) located half way along the side face as near the top surface as possible. The other thermocouple (TC 4) is located on the vertical surface on the free end of the plate at the mid-point of the plate width and as near the top surface as possible.

2.3.3 Post-Process Distorted Shape Measurement Method

In addition to the in situ distortion measurements made using the LDS, the profile of the bottom surface on each plate is also measured before and after processing using a coordinate measurement machine (CMM) to capture the effect of both distortion modes acting simultaneously. As shown in Figure 2.6, nine measurements are obtained both before and after processing at the same locations on the bottom surface of each plate. The difference between the pre- and post-process measurements are used to calculate the plate distortion. These measurements compliment the in situ measurements by providing a means for quantifying both distortion modes experienced by each plate.

2.4 Results

2.4.1 In Situ Deflection

In situ measurements of the distortion caused by each pass and the accumulation of distortion during each clad layer are obtained using the LDS. Figure 2.7 presents the complete deflection history for each case. The longitudinal depositions (Cases 1 through 3) experience a nearly constant accumulation of deflection with respect to processing
Fig. 2.5. Schematics of a) the bottom and b) the top surfaces of the plate showing the LDS measurement target location and the thermocouple locations. TC 3 and TC 4 are welded to the side faces of the plate near the top surface.
time during the deposition of each layer. Transverse depositions (Cases 4 through 6) experience an inconsistent accumulation of deflection during the first layer as the plate begins to deflect downward slightly, then deflects upwards at an increasing rate. The second layer exhibits a similar trend but at a lesser magnitude than the first layer. Table 2.2 presents the total deflection after the completion of the two layers and the deflection that results from each layer. The deflection that occurs during each layer is calculated by subtracting the deflection at the end of the layer from the deflection at the beginning of the layer. The summation of the deflection attributed to each layer does not equal the total deflection because of the slight change in deflection that occurs between layers as the plate cools.

The constant accumulation of deformation experienced in the longitudinal cases (Cases 1-3) and the inconsistent accumulation of deformation in the transverse cases (Cases 4-6) are an artifact of the location of the LDS measurement point. This
(a) Case 1 (Longitudinal pattern, 1.0 kW, 10.6 mm/s)
(b) Case 4 (Transverse pattern, 1.0 kW, 10.6 mm/s)
(c) Case 2 (Longitudinal pattern, 1.0 kW, 4.2 mm/s)
(d) Case 5 (Transverse pattern, 1.0 kW, 4.2 mm/s)
(e) Case 3 (Longitudinal pattern, 2.5 kW, (f) Case 6 (Transverse pattern, 2.5 kW, 10.6 mm/s)

Fig. 2.7. The deflection history of each case.
Table 2.2. The deflection resulting from each layer.

<table>
<thead>
<tr>
<th>Case number</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
</tr>
</thead>
<tbody>
<tr>
<td>Measured distortion mode</td>
<td>Longitudinal bowing</td>
<td>Transverse angular</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Total deflection (mm)</td>
<td>1.74</td>
<td>2.61</td>
<td>3.17</td>
<td>1.17</td>
<td>2.72</td>
<td>2.89</td>
</tr>
<tr>
<td>Contribution from 1st layer (mm)</td>
<td>1.01</td>
<td>1.34</td>
<td>1.76</td>
<td>1.07</td>
<td>2.37</td>
<td>2.27</td>
</tr>
<tr>
<td>Contribution from 2nd layer (mm)</td>
<td>0.71</td>
<td>1.11</td>
<td>1.37</td>
<td>0.00</td>
<td>0.24</td>
<td>0.54</td>
</tr>
<tr>
<td>Difference between layers (%)</td>
<td>30</td>
<td>17</td>
<td>22</td>
<td>100</td>
<td>90</td>
<td>76</td>
</tr>
</tbody>
</table>

behavior is illustrated by comparing the individual passes obtained during representative longitudinal (Case 1) and transverse (Case 4) builds, as shown in Figure 2.8. Each case uses a laser power of 1.0 kW and a travel speed of 10.6 mm/s. In this figure, passes made at the beginning, middle and end of the first layer in the longitudinal and transverse builds are monitored, and the distortion measured during each pass is shown. Every longitudinal deposition pass causes a similar deflection measured by the LDS, as shown in Figure 2.8(a), Figure 2.8(c) and Figure 2.8(e). The plate deflects downward as each pass is deposited because of the expansion of the material as it heats up. After each pass is completed, the plate then deflects upward because of the contraction of the material as it cools. A net increase in the measured deflection occurs after each pass.

On the other hand, the measured mechanical response from the transverse passes in Case 4 exhibits a significantly different behavior. Whereas the longitudinal passes produce a uniform net deflection of the substrate plate, the transverse passes do not. Figure 2.8(b) shows that the early transverse deposition passes do not cause measurable deflection because the passes are deposited between the LDS and the free end of the plate. It is not until a third of the clad layer is completed that measurable deflection
(a) Case 1 (Longitudinal pattern, 1.0 kW, 10.6 mm/s), layer 1, passes 2 and 3.

(b) Case 4 (Transverse pattern, 1.0 kW, 10.6 mm/s), layer 1, passes 2 and 3.

(c) Case 1 (Longitudinal pattern, 1.0 kW, 10.6 mm/s), layer 1, passes 27 and 28.

(d) Case 4 (Transverse pattern, 1.0 kW, 10.6 mm/s), layer 1, passes 57 and 58.

(e) Case 1 (Longitudinal pattern, 1.0 kW, 10.6 mm/s), layer 1, passes 55 and 56.

(f) Case 4 (Transverse pattern, 1.0 kW, 10.6 mm/s), layer 1, passes 114 and 115.

Fig. 2.8. The measured deflection response from different passes in Cases 1 and 4. The gray areas indicate when a pass is being deposited.
is captured by the LDS, as shown in Figure 2.8(d) and Figure 2.8(f). The measured deflection during each pass increases as more passes are deposited between the LDS and the clamp, as shown by the greater deflection in Figure 2.8(f).

2.4.1.1 Deflection From Each Pass

The processing parameters in each case result in different hatch spacing and consequently a different number of passes to clad the same area; therefore, it is advantageous to investigate the distortion caused by each pass. Since each longitudinal pass in Cases 1 through 3 results in approximately the same net increase in measured deflection, the average net deflection caused by each pass is calculated by dividing the deflection resulting from each layer by the number of deposition passes. On the other hand, the deflection in Cases 4 through 6 is a result of the angular distortion caused by each transverse deposition pass, requiring a more in-depth analysis to determine the average deflection. Figure 2.9 shows the relationship between the measured deflection and the average angular distortion caused by each pass in these cases.

It is initially assumed that the angular distortion caused by each transverse pass in a layer is the same. Equation (2.3) is a mathematical expression of the relationship illustrated in Figure 2.9 between the angular distortion caused by each pass and the measured distortion.

\[
D = \sum_{i=1}^{n} d_i = h \sum_{i=1}^{n} \sin((n - i + 1)\phi) \tag{2.3}
\]

where \(D\) is the deflection measured by the LDS, \(h\) is the hatch spacing, \(\phi\) is the average net angular distortion per pass, and \(n\) is the number of passes between the LDS and the
Fig. 2.9. An illustration of the relationship between the measured deflection in the transverse pattern cases (Cases 4–6), the average angular distortion caused by each pass, the hatch spacing and the number of passes deposited between the LDS and the clamp. When the angle is expressed in radians, the small angle theorem can be used to simplify this calculation, resulting in:

\[
D = \sum_{i=1}^{n} d_i = h \sum_{i=1}^{n} (n - i + 1)\phi \tag{2.4}
\]

The average longitudinal bowing deflection caused by each pass in Cases 1 through 3, and the average transverse angular deflection caused by each pass in Cases 4 through 6 are presented in Table 2.3. The angular distortion values calculated for the transverse pattern cases are validated by comparing the accumulation of the calculated distortion resulting from each transverse pass to the measurements, as shown in Figure 2.10. There is good agreement between the predicted deflection and the measured deflection in each case, validating the assumption that the angular distortion caused by each pass in a layer is equal.
(a) Case 4 (Transverse pattern, 1.0 kW, 10.6 mm/s)  
(b) Case 5 (Transverse pattern, 1.0 kW, 4.2 mm/s)  
(c) Case 6 (Transverse pattern, 2.5 kW, 10.6 mm/s)

Fig. 2.10. A comparison of the measured deflection and the calculated deflection using Equation (2.4) in (a) Case 4, (b) Case 5 and (c) Case 6.

Table 2.3. The average deflection caused by each deposition pass.

<table>
<thead>
<tr>
<th>Case number</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
</tr>
</thead>
<tbody>
<tr>
<td>Deflection mode</td>
<td>Longitudinal bowing</td>
<td>Transverse angular</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Average deflection per 1\textsuperscript{st} layer pass</td>
<td>18\textmu m</td>
<td>24\textmu m</td>
<td>53\textmu m</td>
<td>0.016°</td>
<td>0.041°</td>
<td>0.059°</td>
</tr>
<tr>
<td>Average deflection per 2\textsuperscript{nd} layer pass</td>
<td>13\textmu m</td>
<td>19\textmu m</td>
<td>42\textmu m</td>
<td>0.000°</td>
<td>0.040°</td>
<td>0.014°</td>
</tr>
</tbody>
</table>
2.4.2 Post-process Distortion

The CMM measurements are used to calculate the final distortion of the plate. Figure 2.11 presents a polynomial surface fit of the CMM measurements in each case. The data have been rotated so that the slope at CMM 8 in both horizontal axes is zero (i.e. $dz/dx = 0$ and $dz/dy = 0$). The data are then translated so that the distortion at CMM 8 is zero (i.e. $z = 0$ mm). This rotation and translation procedure represents the final distortion of the plate after the deposition of the second layer while it is still clamped into the fixture. An upward curvature is seen along the width and the length axes of the plate in each case.

In addition to the upward curvature, each plate also becomes twisted, as indicated by the unequal deformation of CMM 1 and CMM 3 in each case. Table 2.4 shows the measured distortion at the three CMM points (CMM 1 through 3) along the free end. The longitudinal pattern deposition (Cases 1 through 3) experience greater deflection at CMM 1, which is near the corner where the last deposition pass originates. A positive twist angle is generated in the plate, which is calculated using:

$$\text{twist angle} = \tan \left( \frac{\text{CMM 1} - \text{CMM 3}}{69.85} \right)$$  (2.5)

where 69.85 mm is the distance between the two measurement points. In contrast, the transverse pattern depositions (Cases 4 through 6) experience greater distortion at CMM 3, resulting in negative twist angles of the plate.
Fig. 2.11. A polynomial surface fit of the CMM distortion data after the second layer for each case. The first deposition pass is marked on each surface with an arrow and the LDS measurement point is provided as a reference.
Table 2.4. Distortion of the three CMM measurement points at the free end of the plate in addition to the difference between the two free corners.

<table>
<thead>
<tr>
<th>Case number</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
</tr>
</thead>
<tbody>
<tr>
<td>Deposition Pattern</td>
<td>Longitudinal</td>
<td>Transverse</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Distortion at CMM 1 (mm)</td>
<td>4.21</td>
<td>6.39</td>
<td>7.48</td>
<td>3.49</td>
<td>6.28</td>
<td>5.58</td>
</tr>
<tr>
<td>Distortion at CMM 2 (mm)</td>
<td>3.99</td>
<td>5.76</td>
<td>7.11</td>
<td>3.41</td>
<td>6.16</td>
<td>5.46</td>
</tr>
<tr>
<td>Distortion at CMM 3 (mm)</td>
<td>3.97</td>
<td>5.76</td>
<td>7.22</td>
<td>3.78</td>
<td>6.68</td>
<td>6.29</td>
</tr>
<tr>
<td>Twist angle (°)</td>
<td>0.20</td>
<td>0.52</td>
<td>0.21</td>
<td>-0.24</td>
<td>-0.33</td>
<td>-0.58</td>
</tr>
</tbody>
</table>

2.4.3 Temperature

In addition to the *in situ* deflection measurements captured by the LDS, the thermal histories experienced at selected locations on the substrate are also captured using thermocouple measurements. Figure 2.12(a) presents the temperature history of Case 1 (longitudinal pattern, 1.0 kW, 10.6 mm/s), which is representative of the trends observed in the other longitudinal pattern builds (Cases 2 and 3). Figure 2.12(b) presents the temperature history of Case 4 (transverse pattern, 1.0 kW, 10.6 mm/s), which is representative of the trends observed in the other transverse pattern build (Cases 5 and 6). In all cases, the thermocouples near the top surface (TC 3 and TC 4) experience temperature spikes as the laser passes nearby. These temperature spikes are ignored when analyzing the trends because slight variations in the thermocouple placement can lead to drastic differences in the recorded temperatures at these locations.

The temperatures measured during the longitudinal depositions stabilize after reaching a peak when the laser passes near each thermocouple and do not significantly decrease until the deposition of the layer concludes. Furthermore, the thermocouples
Fig. 2.12. Representative temperature histories resulting from (a) the longitudinal pattern (b) the transverse pattern.
along the plate centerline (TC 1 and TC 4) experience higher temperatures during the longitudinal depositions compared to those placed on or near the edge that coincides with the first longitudinal pass (TC 2 and TC 3). On the other hand, the transverse depositions, as shown in Figure 2.12(b), cause higher temperatures at the mid-point of the plate (TC 1, TC 2, and TC 3) compared to those observed at the free end of the plate (TC 4) that coincides with the first transverse pass. The temperature at each location steadily decreases after the maximum value is achieved. In all cases, the 15 min dwell between layers allows the plate to cool and the heat in the plate to diffuse, resulting in a nearly uniform temperature at the start of the second layer. This temperature is approximately 100 °C higher than that measured at the beginning of the first layer in all cases.

The maximum temperature measured using TC 1, which is located at the center of the bottom surface of the substrate, is used as a standard for comparing the plate temperature across all conditions examined here. Figure 2.13 presents the maximum temperature measured by TC 1 during the deposition of each layer. Two replications of Cases 1, 3 and 5 have been performed to confirm these results. The reported maximum temperatures of these cases are the mean of these replications, while the error bars represent the standard deviation.
2.5 Discussion

2.5.1 The Simultaneous Effect from Both Distortion Modes

The post-process distortion results from this study illustrate the complex distortion generated during laser cladding. Both longitudinal bowing and transverse angular distortion modes occur simultaneously, as demonstrated by the upward curvature along both the width and length of each plate. This observation agrees with the upward deflection resulting from each mode that is measured \textit{in situ} using the LDS. The twist of each plate is dependent on the deposition pattern. The longitudinal passes in Cases 1 through 3 induce a positive twist angle in the plate. The twist angle in these cases is related to laser speed. Cases 1 and 3, in which the laser travels at a speed of 10.6 mm/s, produce similar twists of approximately 0.20°. The slower travel speed of 4.2 mm/s used in Case 2 produces a greater twist angle of 0.52°. In contrast, the twist resulting from the transverse passes in Cases 4 through 6 is correlated with the laser power. Cases 4

Fig. 2.13. A comparison of the maximum temperature measured by TC 1 in each layer.
and 5, which use a power of 1.0 kW, experience a twist of -0.24° and -0.33°, respectively. The higher power of 2.5 kW in Case 6 produces a twist of -0.58°.

### 2.5.2 Correlating Linear Heat Input to Distortion

The *in situ* results indicate that linear heat input, which is a common welding metric related to distortion, is insufficient as a predictive tool for predicting overall distortion experienced during cladding. In welding, decreasing the linear heat input reduces the amount of longitudinal bowing, but the resulting higher temperature gradients may result in a greater amount of transverse angular distortion ([95]). The *in situ* deflection measurements show that the lower linear heat input of 94 J/mm (Case 1) does in fact result in longitudinal bowing deflection per pass that is 25% to 43% lower than the higher linear heat input of 236 J/mm (Cases 2 and 3). However, the lower linear heat input also produces the least amount of transverse angular deflection per pass, as demonstrated by the 94 J/mm transverse deposition in Case 4 that produces 61% to 73% less angular deflection per pass than the 236 J/mm transverse deposition cases (Cases 5 and 6).

The inadequacy of the linear heat input as a metric for cladding is further demonstrated by comparing the deflection resulting from clads that utilize the same linear heat input but two different combinations of power and speed. If linear heat input were adequate, the deflection resulting from the longitudinal bowing mode in Cases 2 and 3 and the transverse angular mode in Cases 5 and 6 would be equal. The lower power and speed combination (1.0 kW and 4.2 mm/s) results in 24 μm of longitudinal bowing deflection per pass (Case 2) and 0.041° of angular deflection per pass (Case 5) during
the first layer. Comparing these deflections to those generated by the higher power and speed combination (2.5 kW and 10.6 mm/s) in Cases 3 and 6, the longitudinal bowing deflection is 55% lower and the transverse angular deflection is 31% lower when the lower power and speed combination is utilized.

In order to predict the deflection from each distortion mode during cladding, additional process parameters must be considered. Considering the similarities between the 236 J/mm cases (Cases 2 and 3 as well as Cases 5 and 6), the significant differences aside from the power and speed are the mass feed rate, hatch spacing, and the number of passes required to clad the area. These variables are all directly related to each other and chosen to produce a good quality clad. For example, hatch spacing and the number of required passes to clad an area are inversely related since more passes are required if the distance between them is smaller. Increasing the mass feed rate increases the layer thickness, but not the pass width (hatch spacing), as described by [96]. The larger hatch spacing of Cases 3 and 6 is a result of the higher laser power of 2.5 kW, which produces a wider pass that can consume the powder at a higher rate than the cases using the lower laser power of 1 kW.

Incorporating hatch spacing into a metric for cladding is desirable since it indicates the density of passes that are deposited over the clad area. Therefore, hatch spacing \( (h) \) is multiplied by linear heat to create the new metric, clad heat:

\[
\text{clad heat} = \frac{Ph}{v} \quad (2.6)
\]
Figure 2.14 illustrates that the deflection generated by each pass in each layer is directly related to this metric. Furthermore, the cumulative deflection from both layers is also related to the clad heat, as shown in Figure 2.15.

![Graph](image)

Fig. 2.14. The proposed metric related to the deflection generated from each pass.

### 2.5.3 The Effect of Pass Count

The temperature measurements show that the total energy balance must be accounted for during the cladding process. Despite the greater amount of energy input into the part using a laser power of 1.0 kW traveling at a speed of 4.2 mm/s (approximately 1800 kJ in Cases 2 and 5) compared to the energy input when a 2.5 kW laser traveling at a speed of 10.6 mm/s is used (approximately 1000 kJ in Cases 3 and 6), there is little difference in the temperature at the center of the bottom surface of the substrate (TC 1), as shown in Figure 2.13. The longitudinal passes deposited in Case 2,
Fig. 2.15. The proposed metric related to the deflection generated from each pass.
*Note that the cumulative and first layer in Case 4 are equal.

which input 1811 kJ per layer, result in a maximum temperature of 325°C during each layer. These temperatures are actually 13 to 15% less than the maximum temperatures measured during each layer of Case 3, in which only 989 kJ of heat is input per layer. Similarly, the maximum temperature resulting from the transverse passes that input 1777 kJ of energy per layer (Case 5) are 4 to 6% less than the maximum temperatures when 1091 kJ of heat in input into the part (Case 6). The extra processing time, which is approximately 3 times greater in Cases 2 and 5 compared to Cases 3 and 6, allows for this additional heat to be evacuated through convection, radiation, and conduction. The greater amount of heat transfer in Cases 2 and 5 may be responsible for the lower deflections despite the higher total energy input compared to Cases 3 and 6.
2.5.4 The Effect of Multiple Clad Layers

The additional deflection resulting from the longitudinal bowing mode is slightly less during the deposition of the second clad layer. Compared to the deflection caused by the first layer, the longitudinal bowing mode causes the plate to deflect by an additional 70 to 83% (Cases 1 through 3) during the second layer. In contrast, transverse angular distortion is insensitive to the deposition of multiple layers. The deposition of a second layer of transverse passes increases the deflection by up to only 24% (Cases 4 through 6) compared to the first layer. In fact, no additional deflection is generated by the second layer deposited using a power of 1.0 kW and a speed of 10.6 mm/s (Case 4). From these results, it is recommended that several thin layers of transverse passes be used to deposit a desired clad thickness instead of few thick layers of longitudinal passes. This strategy is best demonstrated by comparing the deflection resulting from two 0.5 mm thick transverse layers (1.17 mm in Case 4) to the deflection resulting from a single approximately 1 mm thick longitudinal layer (1.34 mm and 1.76 mm for Cases 3 and 4).

2.6 Conclusions

The out-of-plane distortion modes and resulting deflection that is generated during laser cladding Inconel® 625 are investigated using in situ and post-process measurement techniques. The deflection is monitored during the deposition by cantilevering the substrate in a fixture over a laser displacement sensor that measures the vertical deflection of the free end of the substrate. Thermocouples are used to measure the temperature history of the substrate at several locations during the deposition.
The deflection resulting from the longitudinal bowing mode is measured by depositing longitudinal passes, while the deflection caused by the transverse angular mode is measured during the deposition of transverse passes.

The results from this study demonstrate that the distortion experienced during laser cladding is more complex than that observed during equivalent welding processes. The longitudinal bowing and transverse angular distortion modes act simultaneously in response to the deposition passes, causing an upward curvature along both the width and length of the plate. Furthermore, the plate experiences a twist that is dependent upon the deposition pattern. It is shown that the twist resulting from the longitudinal pattern is opposite to the twist resulting from the transverse pattern. The amount of twist is dependent on the laser speed when depositing the longitudinal passes and the laser power when depositing the transverse passes.

These results also show that while linear heat input is a commonly used metric in welding, it alone cannot be used to predict the distortion resulting from laser cladding. A new metric, clad heat, is proposed that incorporates the hatch spacing, in addition to laser power and travel speed. The results show that the deflection generated by each deposition pass, as well as the final deflection, is directly related to clad heat.

In addition, it is shown that even with high heat inputs and long processing times, the maximum temperatures do not exceed those generated by conditions with only half the heat input and shorter processing times. This phenomenon can be attributed to the additional heat loss through convection, radiation, and conduction that occurs from the increased processing time. Considering this, all modes of heat transfer must be considered when performing thermal models of cladding processes.
Finally, it is shown that the deposition of a second layer induces minimal additional transverse angular distortion compared to the first layer. In contrast, the longitudinal bowing distortion during the deposition of the second layer is similar to the distortion generated during the first layer. From this, it is recommended that several thin layers of transverse passes be used in place of fewer thick layers of longitudinal passes to decrease the part deflection resulting from the clad process.

2.7 Acknowledgments

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Chapter 3

Measurement of Forced Surface Convection in Directed Energy Deposition Additive Manufacturing

3.1 Abstract

The accurate modeling of thermal gradients and distortion generated by directed energy deposition additive manufacturing requires a thorough understanding of the underlying physical processes. One area that has the potential to significantly affect the accuracy of thermo-mechanical simulations is the complex forced convection created by the inert gas jets that are used to deliver metal powder to the melt pool and to shield the laser optics and the molten material. These jets act on part surfaces with higher temperatures than those in similar processes such as welding and consequently have a greater impact on the prevailing heat transfer mechanisms. A methodology is presented here which uses hot-film sensors and constant voltage anemometry to measure the forced convection generated during additive manufacturing processes. This methodology is then demonstrated by characterizing the convection generated by a Precetec® YC50 deposition head under conditions commonly encountered in additive manufacturing. Surface roughness, nozzle configuration and surface orientation are shown to have the

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greatest impact on the convection measurements, while the impact from the flow rate is negligible.

3.2 Introduction

In Additive Manufacturing (AM), components are produced in a layer-by-layer manner directly from a digital file. Directed Energy Deposition (DED) is a major subset of currently available AM processes, in which a high energy density heat source, such as a laser, electron beam, or arc, is used to create a melt pool into which metal powder or wire is injected. The localized heat input and the heat removed through conduction, convection, and radiation create complex thermal gradients. When combined with the contraction of the melt pool during rapid solidification, plastic deformation and residual stress are induced and negatively impact the finished part, causing it to crack or distort. Finite Element Analysis (FEA) is often used to simulate the thermal cycles and distortion experienced by discrete components during DED and similar processes. However, useful FEA results can only be achieved when the underlying physical processes are accurately captured.

The high energy density heat source and the resulting distortion modes in DED are very similar to those found in welding. FEA has been used extensively to simulate welding processes [3–5], and many aspects of these studies are useful in defining models of the DED process. Unfortunately, the convection models used to simulate shielding gas flows in welding may not be useful. During welding, the rate of convective heat transfer from the weld bead to the environment is low compared to the rate of conductive heat transfer from the bead into the part [36]. As a result, convective heat loss has been
excluded in some models [6], while natural convection has been applied to all free surfaces in others [7, 8, 11, 97, 98]. However, weld models are not very sensitive to convection because of the comparatively short processing times, low bulk temperatures, and the relatively small amount of filler material addition compared to the size of the existing part.

Even though there are similarities with welding, DED processes have unique characteristics that complicate the convective heat transfer conditions. For example, laser-based processes utilize inert gas jets that are much more concentrated than those used in welding. These jets are used to shield the melt pool, protect the laser optics, and deliver powder to the melt pool. Consequently, localized forced convection is generated on the surfaces of the melt pool and the rapidly solidifying material. In addition, the long processing times and high bulk temperatures prevalent during DED processes allow a great deal of heat to be evacuated through convection [99]. Furthermore, the role of forced convection becomes more significant when the large surface area of the deposition and the localization of the convection are considered.

Michaleris demonstrated that the thermal results of FEA models of DED process are sensitive to surface convection [36]. Despite this sensitivity, convection has been inconsistently applied to FEA models of the process. For example, surface convection has been neglected [32–35] or assumed to be equal to natural convection [18,27,37], much like the conditions used in the welding literature. Dai and Shaw use an atypically high value of 60 W/m²/K for natural convection [65], though the motivation for this choice is unclear. Some of the results from these studies were not validated with experiments,
while others were validated with small builds that did not allow sufficient time for the convective heat loss to become significant.

Other researchers have included forced convection models in their simulations of laser-based DED processes. Qi et al. considered forced convection when modeling laser cladding, however the value of the coefficient of convection was not reported [100]. Michaleris approximated the forced convection as a sphere of higher convection surrounding the melt pool, while free convection was applied on all surfaces outside of this sphere [36]. Ghosh and Choi implemented forced convection from the shielding jet using the empirical equation defined by Gardon and Cobonque [101] and natural convection on all free surfaces not affected by the gas flow [66]. Zekovic et al. included the forced convection on a thin wall and substrate caused by four radially symmetric nozzles blowing an argon gas carrying powder onto the deposition zone [67]. Flow modeling software was used to calculate the velocity contours of the gas around the wall and substrate, and analytical relations were used to calculate local coefficient of convection for the velocity. However, none of these convection models have been validated for the processes being modeled.

Heat transfer resulting from gas jets has been experimentally and numerically studied [44, 49, 52, 57, 58, 102–106]. Many of these works have concluded that the heat transfer is dependent upon the velocity of the jet and its turbulence, which is directly affected by the geometry of the nozzle. Researchers have also investigated how the impingement angle affects the heat transfer on a plate [44, 49, 52, 57]. Others have studied the velocity field and heat transfer resulting from two coaxial jets [58, 102–105] and the effect of particles in the flow [106]. However, these studies are typically performed
using nozzles that are designed to achieve desired flow characteristics, not using nozzles designed for industrial applications. The complexity of the laser deposition process introduces a number of other considerations when attempting to quantify coefficients of convection for improved simulation results. For example, the interaction of the shielding and powder delivery jets, the effect of the metal powder in the jet, and the effect of the deposited surface roughness on the rate of heat transfer have unknown impacts on the predicted convection and the accuracy of these deposition process models.

Measurements of the convection generated during deposition processes are required to produce accurate FEA thermal models [36]. A technique based on the use of hot-film constant voltage anemometry is developed here to measure the heat transfer from the forced convection produced by laser deposition heads. Calibrations are made to then extract the coefficient of convection from the heat dissipated from the sensor. The forced convection is then characterized at multiple locations around the deposition head to generate a map of the forced convection acting on the surface. This technique is demonstrated by characterizing the forced convection from a Precitec\textsuperscript{\textregistered} YC50 deposition head. An integrated distribution of these measurements is compared to the average convection measured using the lumped capacitance method. Convection resulting from jets impinging onto a flat surface or being bisected by a wall is measured to demonstrate the effect of different surface orientations which are common in additive manufacturing. In addition, the effects of changes in gas flow rate, nozzle configuration and surface roughness are investigated to illustrate how convection is affected by changes in common processing parameters.
3.3 Methodology

A new method is presented here that utilizes hot-film sensors and constant voltage anemometry to obtain the coefficient of convection from heat transfer measurements at locations on a flat surface relative to the deposition head. Typically, these sensors are used to measure gas velocity based on empirically derived relationships that are dependent upon the gas properties. In the method presented here, the coefficient of convection is extracted from the direct measurement of the heat transfer from the sensor. When a voltage is supplied by a Constant Voltage Anemometer (CVA) to a hot-film sensor, the heat generated through resistive heating is dissipated into the environment through convection and radiation and into the substrate through conduction. Each sensor must be calibrated to determine the conductive heat transfer and the effective area over which the radiation and convection occur. The results allow the coefficient of convection, $h$, to be extracted from the measurements. The distribution of $h$ on a surface resulting from a gas flow is then characterized by taking measurements at incremental locations relative to the deposition head.

3.3.1 Calculating $h$ from Hot-Film Anemometry Measurements

Constant voltage anemometry is used to measure the steady state heat transfer from the element of a hot-film sensor into its surroundings, as shown in Figure 3.1, and defined by the following energy balance:

$$\int_V Q dV - \int_A q_{\text{conv}} dA - \int_A q_{\text{rad}} dA - \int_A q_{\text{cond}} dA = 0 \quad (3.1)$$
where $Q$ is the heat source generated within the element with a volume, $V_e$, that is dissipated through convection, conduction, and radiation. The corresponding scalar heat fluxes, $q_{\text{conv}}$, $q_{\text{rad}}$ and $q_{\text{cond}}$, are assumed to be uniform over the effective area of the element, $A_e$, as derived empirically in Section 3.3.2.2. Applying an electric current to the element generates heat within it, as defined by:

$$\int_{V_e} Q \, dV = I^2 R_e$$  \hspace{1cm} (3.2)

where the variables $R_e$ and $I$ are the resistance of the element and the current flowing through it. The current is calculated using:

$$I = \frac{V_c}{R}$$  \hspace{1cm} (3.3)

where $V_c$ is the constant voltage applied to the circuit. The total resistance of the circuit, $R$, equals the summation of the resistances of the element, $R_e$, the sensor leads, $R_l$, and the wires connecting the sensor to the anemometer, $R_w$:

$$R = R_e + R_l + R_w$$  \hspace{1cm} (3.4)

The total convective heat flux, $q_{\text{conv}}$, is defined by:

$$\int_A q_{\text{conv}} \, dA = A_e q_{\text{conv}} = h A_e (T_e - T_{\text{amb}})$$  \hspace{1cm} (3.5)
Fig. 3.1. The energy balance of the sensor element.

The variables $T_e$ and $T_{amb}$ are the temperature of the element and the ambient temperature, respectively. The total heat flux through radiation, $q_{rad}$, is:

$$
\int_A q_{rad} \, dA = A \varepsilon A (T_e^4 - T_{amb}^4)
$$

where $\sigma$ is the Stefan-Boltzman constant and $\varepsilon$ is the surface emissivity of the element.

The total amount of heat transferred through conduction, $\tilde{q}_{cond}$, is:

$$
\int_A q_{cond} \, dA = \tilde{q}_{cond}
$$

The total conductive heat transfer is derived empirically in Section 3.3.2.1. As a result, the total energy balance of the sensor is:

$$
I^2 R_e - A \varepsilon A q_{conv} - A \varepsilon A q_{rad} - \tilde{q}_{cond} = 0
$$
The total energy balance is maintained despite changes in the heat transfer acting on the sensor element because its resistance changes in response. For example, an increase in the convection acting on the element decreases its temperature. In turn, both the element resistance and the total resistance of the circuit decrease, causing the current flowing through the element to increase and leading to more resistive heating that balances the increase in convective heat transfer. The following equation relates the temperature and resistance of the element:

\[
\Delta T = T_e - T_{amb} = \frac{R_e}{R_{amb}} - 1
\]  

(3.9)

The resistance increases from its initial value, \( R_e \) at ambient temperature to the value during the measurement according to the increase in temperature, \( \Delta T \), and the temperature coefficient of resistance of the element, \( \alpha \). The initial resistance of the element must be determined before the measurement. The total resistance of the circuit during the measurement is calculated from the anemometer output signal.

Based on these measurements, the coefficient of convection can be determined using the following relationship:

\[
h = \left( \frac{V}{R} \right)^2 \left( R - R_l - R_w \right) - \varepsilon \sigma (T_{amb}^4 + \Delta T_{amb}^4 - T_{amb}^4) - \tilde{q}_{\text{cond}} \frac{A_e \Delta T}{A_e} \]  

(3.10)
The standard deviation of the coefficient of convection, $s_h$, is calculated using the propagation of error formulation [107]:

$$ s_h = \sqrt{S_V + S_R + S_{\tilde{q}} + S_A + S_{\Delta T}} \tag{3.11} $$

$$ S_V = \left( \frac{\partial h}{\partial V} \right)^2 s_{V}^2 \tag{3.12} $$

$$ S_R = \left( \frac{\partial h}{\partial R} \right)^2 s_{R}^2 \tag{3.13} $$

$$ S_{\tilde{q}} = \left( \frac{\partial h}{\partial \tilde{q}_{\text{cond}}} \right)^2 s_{\tilde{q}}^2 \tag{3.14} $$

$$ S_A = \left( \frac{\partial h}{\partial A_e} \right)^2 s_{A}^2 \tag{3.15} $$

$$ S_{\Delta T} = \left( \frac{\partial h}{\partial \Delta T} \right)^2 s_{\Delta T}^2 \tag{3.16} $$

where $s_V$ and $s_R$ are the standard deviations of the applied constant voltage and the calculated circuit resistance during the measurement, respectively. The standard deviation of the element temperature, $s_{\Delta T}$, is also derived using the propagation of error formulation:

$$ s_{\Delta T} = \sqrt{\left( \frac{\partial \Delta T}{\partial R_e} \right)^2 s_{R}^2} \tag{3.17} $$
where $s_{Re}$ is the standard deviation of the element resistance during the measurement. The variables $s_{\tilde{q}_{\text{cond}}}$ and $s_{A_e}$ are the standard deviations of the differences between the calibration data and the empirically derived functions for the conduction, $\tilde{q}_{\text{cond}}$, and the effective area, $A_e$.

### 3.3.2 Sensor Calibration

#### 3.3.2.1 Conduction into the substrate

Conduction from the sensor element into the substrate is complex and cannot be easily calculated. Both the thin-film onto which the sensor element is deposited and the adhesive used to mount the sensor to the substrate impact the conduction in unknown ways. Empirically derived functions must therefore be developed to take into account the variations within the sensors, adhesives and substrates.

In order to measure the conduction, the sensor must be insulated from convection and radiation. Insulation is used to eliminate the convective and radiative heat transfer components and allow conduction to be measured using the following relationship:

$$I^2 R_e - \tilde{q}_{\text{cond}} = 0 \quad (3.18)$$

A range of constant voltages are applied to the sensor to heat the element to different temperatures. Measurements of the heat transfer from the element are made at each constant voltage increment once the element temperature has stabilized. It is assumed that the substrate temperature is equal to the ambient temperature, $T_{\text{amb}}$, and does not
change during the calibration since its thermal mass is orders of magnitude larger than the thermal mass of the element.

The average values of the constant voltage and the output signal during the steady-state measurement at each increment are used to calculate the increase in sensor element temperature, $\Delta T$. These values are calculated using the CVA supplied function to calculate the circuit resistance from the output signal and Equation (3.9). Conductive heat transfer is calculated using Equation (3.18). A function is then fit to the data to relate conductive heat transfer to $\Delta T$. The standard deviation of the difference between the data and the fit is used to calculate $\tilde{s}_{q_{\text{cond}}}$.

### 3.3.2.2 Effective surface area of the element

Resistive heating within the sensor element generates a greater temperature in the element than in the leads. This temperature gradient effectively spreads the heat over a greater area than the nominal area of the element, as demonstrated by O’Donovan et al. [108] using a FEA model of a sensor. In the method presented here, the effective surface area is empirically derived (as a function of $\Delta T$) by performing a series of measurements of the heat transfer at a variety of constant voltages while the substrate is placed in a steady and uniform gas flow, hereafter known as the reference flow. The convection generated by the reference flow is measured using the lumped capacitance method [109]. In this method, a thin plate of a known material and geometry is heated to a uniform temperature and then placed in the reference flow. The temperature of the plate is measured as it cools and is used to calculate the average coefficient of convection, $\bar{h}$, acting over the plate. The cooling of the plate with a known volume, $V$, density, $\rho$,
and heat capacity, $c$, subjected to convection and radiation is described below:

$$\bar{h}A(T - T_{amb}) + \varepsilon\sigma A(T^4 - T_{amb}^4) = -\rho c V \frac{dT}{dt}$$  \hspace{1cm} (3.19)$$

where $A$ is the exposed surface area of the plate through which convection and radiation occur. Since Equation (3.19) is non-linear, $\bar{h}$ is calculated using an iterative process to achieve a decay in $T$ that matches the measured temperature decay. Once the average coefficient of convection is found, it can be substituted for $h$ so that the effective surface area can be calculated.

The lumped capacitance method is valid when the ratio of convection to internal conduction is sufficiently small. This ratio is described by the non-dimensional Biot number, $Bi$:

$$Bi = \frac{\bar{h} V}{Ak}$$  \hspace{1cm} (3.20)$$

where $k$ is the thermal conductivity of the plate. The lumped capacitance method is a valid way to measure the average coefficient of convection only when $Bi$ is less than 0.1 [109]. The Biot number is calculated from all lumped capacitance measurements to prove their validity.

### 3.3.3 Map the Distribution of Convection

The distribution of the localized forced convection generated by a deposition head is characterized using measurements made at incremental locations, as shown in Figure 3.1. When the gas flow is begun, the sensor is allowed to achieve equilibrium, as defined in Equation (3.8). The time required for this to occur is dependent upon the sensor and
substrate. Once the output signal from the CVA becomes steady, the constant voltage and the sensor output are recorded, then the mean values and standard deviations of these signals are used to calculate the coefficient of convection, $h$, and the standard deviation of this measurement, $s_h$, at this location. The relative position of the deposition head to the sensor is then incrementally changed and time is allowed for the signals to become steady once again before the signals are recorded, allowing $h$ and $s_h$ for the new location to be calculated. This procedure is repeated until the convection distribution acting over the desired surface area is mapped.

3.4 Demonstration and Verification of the Methodology

3.4.1 Equipment

Demonstration of the convection measurement method is performed using a commercially available Precitec® YC50 clad head and nozzles, which are schematically depicted in Figure 3.2. In this specific design, argon gas flows through the center of the clad head to shield the melt pool and to protect the laser optics positioned upstream from the head. The inner nozzle constricts the flow from a diameter of 42.0 mm at the interface with the head to a diameter of 10.4 mm at the nozzle exit over a length of 60.1 mm, allowing the shield flow to exit the nozzle through an 85 mm$^2$ area. Powder and argon gas are delivered to the part through a 2.5 mm wide circumferential channel in the clad head. The clearance between the outer nozzle and the inner nozzle is 0.7 mm, and the powder delivery flow exits through a 65 mm$^2$ area between the inner and outer
nozzles. During processing, the bottom of the outer nozzle is positioned at a constant height of 10 mm above the target surface.

Senflex® SF9902 single-element hot-film sensors from Tao of Systems Integration, Inc. are used to measure the convective heat transfer generated by the deposition head. Figure 3.3 shows a schematic diagram of a characteristic sensor that consists of a nickel element and copper leads. The nickel element, which is nominally 0.20 μm thick, 0.1 mm wide, and 1.4 mm long has been deposited onto 0.2 mm thick Upilex® polyimide film. Copper leads that are nominally 12 μm thick and 0.76 mm wide are also deposited onto the film. Copper wires with diameters of 0.25 mm and lengths between 3.3 m and 3.6 m are used to connect the sensors to a 4 channel Tao of Systems Integration, Inc. Model 4-600 CVA. The relationship between the output voltage, $V_o$, and the total resistance of the circuit, $R$, is given below [110]:

$$R = \frac{1}{\frac{a}{V} + b}$$

(3.21)

where $a$ and $b$ are channel-specific variables that are provided with the CVA [110]. The voltage signals from the CVA are acquired using a National Instruments NI 9205 module. Type-K thermocouples are used to monitor the ambient temperature of the environment and to measure the temperature of the argon jets at the nozzle outlet. The jet temperature is measured before the convection measurements so that the thermocouple does not affect the flow during those measurements. It is found that the jet temperature is equal to the ambient temperature of the deposition environment.
Fig. 3.2. A cross-sectional view of the Precitec\textsuperscript{\textregistered} YC50 head and nozzles.
A National Instruments NI 9213 module is used to acquire the thermocouple signals. The acquired thermocouple and voltage signals are recorded in a Labview® environment at a rate of 20 Hz.

Fig. 3.3. The dimensions of the nickel element and copper leads of a Senflex® SF9902 hot-film sensor.

Three sensors are attached to a 222 mm long, 123 mm wide and 3.2 mm thick plexiglass substrate using MACfilm® IF-2012 adhesive, as shown in Figure 3.4. This configuration allows the convection acting on a wall to be measured at different distances from the top edge when the substrate is mounted vertically. A $45^\circ$ edge is ground into the back edge of the substrate so that this edge has a minimal impact on the gas flow when the substrate is mounted vertically to bisect the flow. Sensors 1 and 2 are placed 0.8 mm and 5.4 mm from this edge, respectively. Sensor 3 is mounted near the middle of the substrate, 108.7 mm from the ground edge.
3.4.1.1 Calibrating the Senflex® Sensors

Heat loss through conduction  The process described in Section 3.3.2.1 is used to empirically derive functions that describe the conductive heat loss from each sensor into the plexiglass substrate. Fiberglass insulation, 90 mm thick, is placed on top of the substrate. Measurements are made using constant voltages between 0.3 V and 1.0 V, in 0.1 V increments, to heat the nickel elements. At each setting, the energy balance is allowed approximately 30 s to reach equilibrium before the $V_c$ and $V_o$ signals are recorded over a period of approximately 30 s. The mean values of these signals are used to calculate $\Delta T$ and $\tilde{q}_{\text{cond}}$ at each increment. Figure 3.5 presents the results from these measurements. A second order polynomial function produced the best fit to the data for each sensor, as shown in the following relationships:

$$\tilde{q}_{\text{cond,Sensor 1}} = -2.428e^{-7} \Delta T^2 + 3.854e^{-4} \Delta T - 2.876e^{-3} \quad (3.22)$$
\[
\dot{q}_{\text{cond, Sensor}_2} = -2.174e^{-7} \Delta T^2 + 4.072e^{-4} \Delta T - 3.491e^{-3}
\]

\[
\dot{q}_{\text{cond, Sensor}_3} = -4.489e^{-7} \Delta T^2 + 5.241e^{-4} \Delta T - 1.149e^{-3}
\]

The quality of these fits are described by the difference between the calculations and measurements at one standard deviation. These values are less than 1% of the rate of conduction into the substrate from each sensor.

**Effective surface area**  The effective area of each sensor is found using the process described in Section 3.3.2.2 using aluminum, copper, and steel plates. Different plates are used to demonstrate that this calibration is independent of the chosen plate material. Table 3.1 presents the properties of these plates. Each plate is heated and then placed on an insulated fixture, to restrict the heat loss to one side of the plate, and cooled in four different air flows. The chosen reference flows have average velocities that are similar to the output from the deposition head. Three of the reference flows, with velocities of 1.3 m/s, 1.8 m/s, and 2.5 m/s, are parallel to the plate. A fourth reference flow with a velocity of 2.8 m/s is perpendicular to the cooling plate.

Table 3.2 presents the results of the modified lumped capacitance measurements. The negligible differences between the measurement made using each plate indicates that the plate material has no impact on the calibration. The mean and standard deviation of the average coefficient of convection is calculated for each air flow. Considering the difference in properties of the three plates, the small standard deviation in each air flow illustrates the consistency of the lumped capacitance method to determine the average convection resulting from each reference flow condition.
Fig. 3.5. For each sensor, the heat loss through conduction is dependent on the element temperature.
Table 3.1. The properties of the plates used in the modified lumped capacitance analysis to determine the effective surface area of each sensor element.

<table>
<thead>
<tr>
<th></th>
<th>Al 6061-T6</th>
<th>Commercially pure Cu</th>
<th>A36 steel</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\rho$ (kg m$^{-3}$)</td>
<td>2710</td>
<td>8470</td>
<td>7833</td>
</tr>
<tr>
<td>$c$ (J kg$^{-1}$ K$^{-1}$)</td>
<td>896</td>
<td>397</td>
<td>465</td>
</tr>
<tr>
<td>$k$ (W m$^{-1}$ K$^{-1}$)</td>
<td>167</td>
<td>386</td>
<td>54</td>
</tr>
<tr>
<td>$A$ (m$^2$)</td>
<td>20.5e-3</td>
<td>19.8e-3</td>
<td>19.5e-3</td>
</tr>
<tr>
<td>$V$ (m$^3$)</td>
<td>65.2e-6</td>
<td>62.8e-6</td>
<td>124e-6</td>
</tr>
<tr>
<td>$L$ (mm)</td>
<td>3.18</td>
<td>3.17</td>
<td>6.36</td>
</tr>
<tr>
<td>$\epsilon$</td>
<td>0.09</td>
<td>0.038</td>
<td>0.79</td>
</tr>
</tbody>
</table>

Table 3.2. The results of the modified lumped capacitance measurements to determine the effective surface area of each sensor element.

<table>
<thead>
<tr>
<th></th>
<th>Parallel to a 1.3 m/s air flow</th>
<th>Parallel to a 1.8 m/s air flow</th>
<th>Parallel to a 2.5 m/s air flow</th>
<th>Perpendicular to a 2.8 m/s air flow</th>
</tr>
</thead>
<tbody>
<tr>
<td>Plate</td>
<td>Al</td>
<td>Cu</td>
<td>Steel</td>
<td>Al</td>
</tr>
<tr>
<td>$\bar{h}$ (W/m$^2$/K)</td>
<td>27.5</td>
<td>25.4</td>
<td>26.6</td>
<td>32.5</td>
</tr>
<tr>
<td>$Bi$</td>
<td>5e-4</td>
<td>2e-4</td>
<td>4e-3</td>
<td>7e-4</td>
</tr>
<tr>
<td>Average $\bar{h}$ (W/m$^2$/K)</td>
<td>27 ± 1</td>
<td>31 ± 2</td>
<td></td>
<td>38 ± 1</td>
</tr>
</tbody>
</table>
After the average convection resulting from each reference flow is determined, measurements are made using the hot-film sensors with the substrate placed in the insulated fixture. Measurements are made in each air flow using constant voltages, $V_c$, ranging from 0.3 V to 1.0 V in 0.1 V increments, to heat the element to a range of temperatures. The mean value of each signal and the average coefficient of convection from Table 3.2 is used to calculate the effective area using Equation (3.5) for each flow condition. Figure 3.6 presents the resulting data to which second order exponential equations are fit to the data to develop functions to describe $A_e$ for each sensor, as well as the standard deviations of the difference between the data and these functions:

$$A_{e,\text{Sensor 1}} = 5.66 \exp^{-0.017\Delta T} + 4.15 \exp{-0.003\Delta T}$$ (3.25)

$$A_{e,\text{Sensor 2}} = 22.45 \exp{-0.065\Delta T} + 5.48 \exp{-0.005\Delta T}$$ (3.26)

$$A_{e,\text{Sensor 3}} = 3.02 \exp{-0.013\Delta T} + 0.56 \exp{-0.001\Delta T}$$ (3.27)

### 3.4.1.2 Measurement Cases

Table 3.3 presents the hot-film constant voltage anemometry measurement cases and Figure 3.7 illustrates the different measurement setups used to map the convection generated by the deposition head. The hot-film measurement method is used to map the distribution of $h$ resulting from the argon jet impinging onto a surface in Cases 1 through 5. The experimental setup is presented in Figure 3.7(a). The plexiglass substrate is placed on the X-Y plane, with the Sensor 3 located at the origin of the reference frame.
Fig. 3.6. The measurement data and the corresponding 2\textsuperscript{nd} order exponential fit to used calculate the effective area for each sensor.
Powder is not included during these hot-film measurements because its abrasive nature could deteriorate the nickel element, changing its resistance and affecting the calculation of the coefficient of convection according to Equation (3.10). Both the shielding and powder delivery flows are used in Cases 1 and 2, with rates of 9 L/min and 19 L/min, respectively. Cases 3 and 4 are performed to investigate the heat transfer when the head is used for laser processes that do not require powder to be injected into the melt pool, such as wire-based deposition. The rate of the shielding flow in Case 3 is 9 L/min and the rate in Case 4 is 19 L/min.

Table 3.3. The cases used to measure the distribution of $h$ resulting from different argon flow conditions.

<table>
<thead>
<tr>
<th>Case</th>
<th>Jet</th>
<th>Surface</th>
<th>Shielding flow (L/min)</th>
<th>Powder delivery flow (L/min)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Impinging</td>
<td>Smooth</td>
<td>9</td>
<td>9</td>
</tr>
<tr>
<td>2</td>
<td>Impinging</td>
<td>Smooth</td>
<td>19</td>
<td>19</td>
</tr>
<tr>
<td>3</td>
<td>Impinging</td>
<td>Smooth</td>
<td>9</td>
<td>0</td>
</tr>
<tr>
<td>4</td>
<td>Impinging</td>
<td>Smooth</td>
<td>19</td>
<td>0</td>
</tr>
<tr>
<td>5</td>
<td>Impinging</td>
<td>Rough</td>
<td>9</td>
<td>9</td>
</tr>
<tr>
<td>6</td>
<td>Parallel</td>
<td>Smooth</td>
<td>9</td>
<td>9</td>
</tr>
</tbody>
</table>

In Case 5, Shurtape® anti-skid tread tape is applied to the plexiglass substrate to simulate the rough surface produced typically produced in a powder-based deposition. Figure 3.8 shows the similarity of a powder-clad surface [99] and the tread tape. For the purpose of this study, the roughness of the tape does not need to be identical to that of
75

(a) The argon jet imping upon the plexiglass substrate. 
(b) The plexiglass substrate bisects the argon jet. 

Fig. 3.7. Illustrations of the experimental setups. (a) The distribution of \( h \) resulting from an impinging jet is measured in Cases 1 through 5. (b) The distribution of \( h \) on a vertical wall when it bisects the argon jet is measured in Case 6.
a clad surface, it is only used to demonstrate the potential effect that a rough surface, similar to one produced through powder cladding, has on the rate of convection.

![Clad Surface and Anti-Skid Tape](image)

(a) A clad surface from [99]. (b) The Shurtape anti-skid tread tape.

Fig. 3.8. Images of (a) the clad surface produced in reference [99] using a laser power of 2.5 kW, a travel speed of 10.6 mm/s, a hatch spacing of 2 mm and a powder flow delivery rate of 19 g/min and of (b) the Shurtape anti-skid tread tape that is applied to the surface around the sensor to approximate the clad surface.

In Case 6, the distribution of $h$ on the face of a vertical wall resulting from a parallel jet is measured using the hot-film method. As shown in Figure 3.7(b), the shielding and powder delivery jets are used and the top edge of the substrate, with the $45^\circ$ edge, bisects the argon jet. The convection acting at increasing distances from the top edge is measured using the three sensors.

### 3.4.2 Verification of the Methodology

Modified lumped capacitance measurements are performed, using copper and aluminum plates, to verify the hot-film measurement method. Both argon flows are used to create an impinging jet to cool the plates, and the rate of each flow is 9 L/min.
Each plate is heated to a temperature greater than 100 °C then placed on an insulated fixture under the deposition head, as shown in Figure 3.9. Measurements are made with and without the inclusion of powder in the flow to determine its impact on the measurement of the average coefficient of convection.

![Fig. 3.9. An illustration of the modified lumped capacitance method.](image)

**3.4.2.1 The effect of metal powder in the flow**

It is necessary to determine the impact of powder on the coefficient of convection because it cannot be used during the hot-film anemometry measurements. Table 3.4 presents the average coefficient of convection, \( \bar{h} \), measured using a modification of the lumped capacitance method, for the coaxial jets impinging upon the surface of each metal plate. The jets are produced by the two argon flows, each with a rate of 9 L/min (Case 1). When no powder is included in the argon flow, the measured values of \( \bar{h} \) are 65.6 W/m\(^2\)/K using the aluminum plate and 66.5 W/m\(^2\)/K using the copper plate.
When Inconel® 625 powder, with a particle size distribution between 44 µm and 149 µm, is delivered at a rate of 19 g/min, the measured values of $\bar{h}$ are 65.1 and 65.4 W/m$^2$/K. The inclusion of powder decreases $\bar{h}$ by only 0.7% and 1.6% for each plate, indicating that the powder has a negligible effect on the average convective heat transfer.

Table 3.4. The values of $\bar{h}$ measured using the lumped capacitance method, resulting from an impinging jet created by a 9 L/min shield flow and a 9 L/min powder delivery flow (Case 1).

<table>
<thead>
<tr>
<th>Material</th>
<th>Dimensions (mm)</th>
<th>Powder rate (g/min)</th>
<th>$\bar{h}$ (W/m$^2$/K)</th>
<th>Bi</th>
</tr>
</thead>
<tbody>
<tr>
<td>Al 6061-T6</td>
<td>49.33×46.43×3.12</td>
<td>0</td>
<td>65.6</td>
<td>1e$^{-3}$</td>
</tr>
<tr>
<td>Commercially pure Cu</td>
<td>50.01×50.08×3.18</td>
<td>19</td>
<td>65.1</td>
<td>1e$^{-3}$</td>
</tr>
</tbody>
</table>

### 3.4.2.2 Comparison of results obtained using the different methods

The surface convection resulting from an impinging jet is characterized by taking measurements at discrete points on the X- and Y-axes. By compiling these individual measurements, the distribution of the coefficient of convection can be compared to the average coefficient of convection measured using the lumped capacitance method. Figure 3.10 presents the hot-film measurement results from Case 1 in which both the shielding and powder delivery argon jets impinge upon the substrate at a rate of 9 L/min. Figure 3.10(a) shows that $h$ decays from a maximum value of 105 ± 9 W/m$^2$/K at the impingement point (X=0 mm) to a value of 32 ± 3 W/m$^2$/K at the edges. The decrease
in $h$ is more gradual on the negative X-axis than on the positive X-axis. Figure 3.10(b) shows that two local maxima of $96 \pm 7 \text{ W/m}^2/\text{K}$ and $117 \pm 8 \text{ W/m}^2/\text{K}$, separated by 12 mm, occur on the Y-axis. A minimum value of $75 \pm 6 \text{ W/m}^2/\text{K}$ is measured between these two maxima.

The hot-film measurement results are compared to those obtained using the lumped capacitance method. To make this comparison, the average coefficient of convection, $\bar{h}$, acting over the area corresponding to the surface of each metal plate is calculated. First, a quadratic polynomial fit is applied to the data and $\bar{h}$ is calculated as the average of this surface fit. The anemometry measurements produce values that are 3 to 5% greater than those measured using the lumped capacitance method. This small difference can be attributed to the nature of the two measurement methods. During the lumped capacitance method, the argon jet impinges upon the metal plate, causing the gas to deflect and flow across its surface. As it flows over the surface, the temperature of the gas increases as it extracts heat from the plate, reducing the rate of heat transfer acting nearer the edges of the plate. In the anemometry measurements the gas does not extract heat from the plexiglass substrate as it flows towards the heated sensor element at each measurement location.

### 3.5 Measurement Results and Discussion

The impact of flow rate on convection is investigated by first comparing two cases with different argon flow rates supplying each jet. Figure 3.11 compares the results measured in Case 1 with those obtained in Case 2, in which both the shielding and powder delivery argon jets impinge upon the substrate at a rate of 19 L/min. This flow
Fig. 3.10. The results from Case 1 in which the shielding and powder delivery jet impinge upon the substrate. The rate of each flow is 9 L/min. Measurements are performed in 3 mm increments along (a) the X-axis and (b) the Y-Axis.
rate is approximately twice the flow rate used in Case 1. The value of $h$ resulting from the 19 L/min flows decays from a value of $107 \pm 8 \text{ W/m}^2/\text{K}$ to a value of approximately $34 \text{ W/m}^2/\text{K}$ at the outermost measurement locations. The decrease in $h$ is more gradual on the negative axis than the positive axis.

Increasing the flow rate supplying each jet has little effect on the convection generated by the deposition head. Figure 3.11 shows that increasing the rate of each flow from 9 L/min in Case 1 to 19 L/min in Case 2 increases the maximum measured value of $h$ by only 2 W/m$^2$/K. In addition, the values of $h$ measured at the outermost locations also increase by 1 W/m$^2$/K. These differences are within the uncertainty of the measurements.

Figure 3.12 presents the results from Cases 3 and 4 in which only the shielding jet impinges upon the substrate at a rate of 9 L/min and 19 L/min, respectively. In Case
3, two local maxima with values of $39 \pm 4 \text{ W/m}^2/\text{K}$ and $41 \pm 4 \text{ W/m}^2/\text{K}$ are separated by 15 mm with a local minimum of $28 \pm 3 \text{ W/m}^2/\text{K}$ between them. The convection decays from these maxima to approximately $19 \text{ W/m}^2/\text{K}$ at the outermost locations. For Case 4, values of $52 \pm 4 \text{ W/m}^2/\text{K}$ and $53 \pm 5 \text{ W/m}^2/\text{K}$ are measured at the two local maxima. These maxima are separated by 15 mm, in which a local minimum of $34 \pm 4 \text{ W/m}^2/\text{K}$ is measured. The convection decays from these peaks to approximately $21 \text{ W/m}^2/\text{K}$ at the outer measurement locations.

Fig. 3.12. A comparison of the convection generated by only the shielding jet using different flow rates.

Increasing the rate of the shielding flow from 9 L/min in Case 3 to 19 L/min in Case 4 increases the values of $h$ measured at the peaks by $12 \text{ W/m}^2/\text{K}$. This increase exceeds the measurement uncertainty. The minimum value between the peaks increases by $6 \text{ W/m}^2/\text{K}$. However, there is no difference in the measured values of $h$ at the
measurement extremes, indicating that changes to the flow rate have no impact on the convection acting away from the deposition head.

Figure 3.13 presents a comparison of the heat transfer caused by a single impinging jet compared to two coaxial impinging jets. The combined flow rate of the two jets in Case 1 is 18 L/min and the flow rate of the shielding jet in Case 4 is 19 L/min. Despite these similar flow rates, the distribution of $h$ in these two cases is very different. The two coaxial jets in Case 1 create an asymmetric distribution of $h$ with a single peak. The single jet in Case 4 creates a symmetric distribution with two peaks. The values of $h$ measured at the peaks in Case 4, which uses only the shielding flow, are 50% less than the peak when both flows are employed in Case 1.

![Graph showing heat transfer comparison](image)

**Fig. 3.13.** The convection generated by both the shielding and powder delivery jets compared to the convection generated by only the shielding jet.

Although the flow rates of the shielding and powder delivery jets are the same in Case 1, the average velocity of the shielding jet as it exits the inner nozzle is 1.8 m/s
whereas the average velocity of the powder delivery jet as it exits the outer nozzle is 2.3 m/s. This difference in velocity creates a shear between the jets that alters the flow structure, as shown by Hwang et al. [58]. In that study, two coaxial jets with different exit velocities created vortices so that their effect on heat transfer could be investigated. It was shown that the induced vortices could increase the rate of heat transfer on the surface. This effect explains the increased distribution of $h$ when both jets are used in Case 1 compared to when only the shielding jet is used in Case 4.

The impact of surface roughness on convection is investigated by comparing Case 1 to Case 5, as shown in Figure 3.14. In each case, both argon jets impinge upon the surface at a rate of 9 L/min. In Case 5, in which the surface surrounding the sensor is rough, the value of $h$ decays from $88 \pm 7$ W/m$^2$/K to approximately 21 W/m$^2$/K. Compared to the smooth surface in Case 1, the rough surface in Case 5 decreases the maximum value of $h$ by 19 W/m$^2$/K compared to the maximum value in Case 1. In addition, the rough surface causes $h$ to decrease more rapidly from the peak value. Finally, the rough surface decreases the measured values of $h$ at the outer locations by 11 W/m$^2$/K.

The distribution of convection acting on a vertical wall is found by analyzing the results of Case 6. Figure 3.15 presents the results from Case 6 in which both argon jets flow parallel to the substrate at a rate of 9 L/min. The jets flow parallel to the wall surface and are bisected by the top edge of the wall. The coefficient of convection is measured at three locations on the wall. The maximum value of $h$ measured by Sensor 1, which is located only 0.8 mm from the top edge of the wall, is $58 \pm 4$ W/m$^2$/K. At Sensor 2, which is 5.4 mm below the top edge, the maximum measured value is $40 \pm 6$ W/m$^2$/K. The distribution measured by each of these sensors exhibit a second peak of
a significantly lesser value than the maximum. The value of $h$ measured by Sensor 1 at the extreme locations is $25 \pm 2$ W/m$^2$/K and the values measured by Sensor 2 is $22 \pm 3$ W/m$^2$/K. The distribution of $h$ measured by Sensor 3 exhibits a different trend. A maximum value of $36 \pm 4$ W/m$^2$/K, which decays to values of approximately 21 and 32 W/m$^2$/K, is measured by Sensor 3.

Figure 3.16 compares the distribution of $h$ resulting from an impinging jet (Case 1) and from a bisected parallel jet (Case 6). The flow rate of each jet in both cases is 9 L/min. The comparison is made using Sensor 1 in Case 6, which is is closest to the top edge of the plate. The impinging jet causes a greater rate of heat transfer than the parallel jet, which is evident by the fact that the maximum value of $h$ in Case 1 is 81% greater than the maximum value in Case 6. The distribution of $h$ is different between these two cases. A double peak is created by the parallel flow in Case 6 whereas a single peak is created by the impinging jet in Case 1. In the literature, impinging jets which impinge directly onto a surface have been found to induce a greater rate of heat transfer.
Fig. 3.15. The convection acting on a vertical wall in Case 6.
compared to parallel jets or those with a shallower impingement angle [44, 49, 52, 57].

The jet in Case 1 is deflected by the surface and spread radially from the impingement point, which results in a higher value of $h$ spread over a greater area. In contrast, the wall bisects the parallel jet and does not disrupt the flow to the same extent.

![Graph showing the comparison of convection on a horizontal surface and near the top edge of a vertical wall.]

Fig. 3.16. A comparison of the convection on a horizontal surface compared to the convection near the top edge of a vertical wall.

### 3.6 Conclusions

A method using hot-film constant voltage anemometry to measure the heat transfer from the sensor into the environment is developed to characterize the surface convection contribution from the shielding and powder delivery systems used in laser-based additive manufacturing processes. Calibrations are performed so that the coefficient of convection can be extracted from the heat transfer measurement. The distribution of the forced convection is mapped by incrementally taking measurements.
at different locations relative to the deposition head. By comparing its results to those obtained using a modified lumped capacitance measurement, the method is shown to be valid. The modified lumped capacitance measurement is also used to demonstrate that excluding powder during the hot-film anemometry measurement has little impact on the results.

The method is then demonstrated by characterizing the convection generated by a Precitec® YC50 deposition head. It is found for this application that the distribution of convection is affected by a variety of factors:

1. Increasing the flow rate minimally affects the surface convection.

2. The surface orientation significantly affects the distribution of convection. The convection generated on a wall is more concentrated and has a lower magnitude than compared to the convection acting on a horizontal surface.

3. Convection is dependent upon the nozzle configuration. More convection is generated when the powder delivery flow is included in the jet, as would be used during a powder deposition, than when only the shielding jet is used, as would be used during a wire deposition.

4. A rough surface decreases the convection distribution.

These results are specific for the deposition head used in this study. The hot-film constant voltage anemometry method must be used to measure the convection generated by other deposition heads, since the design of other deposition heads could affect the gas flow and alter the convection distribution.
3.7 Acknowledgments

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4.1 Abstract

A thermo-mechanical model of directed energy deposition additive manufacturing of Ti-6Al-4V is developed using measurements of the surface convection generated by gasses flowing during the deposition. In directed energy deposition, material is injected into a melt pool that is traversed to fill in a cross-section of a part, building it layer-by-layer. This creates large thermal gradients that generate plastic deformation and residual stresses. Finite element analysis (FEA) is often used to study these phenomena using simple assumptions of the surface convection. This work proposes that a detailed knowledge of the surface heat transfer is required to produce more accurate FEA results. The surface convection generated by the deposition process is measured and implemented in the thermo-mechanical model. Three depositions with different geometries and dwell times are used to validate the model using in situ measurements of the temperature and deflection as well as post-process measurements of the residual stress. An additional model is developed using the assumption of free convection on all

\[1\text{The work presented in this chapter has been Published: Heigel, J.C., Michaleris, P., Reutzel, E.W. "Thermo-Mechanical Model Development and Validation of Directed Energy Deposition Additive Manufacturing of Ti-6Al-4V." Additive Manufacturing (2014). doi:10.1016/j.addma.2014.10.003.}\]
surfaces. The results show that a measurement-based convection model is required to produce accurate simulation results.

4.2 Introduction

Directed Energy Deposition (DED) [111] is an additive manufacturing process that creates parts through the layer-by-layer addition of material. DED uses a high intensity energy source, such as a laser, to create a melt pool into which metal powder or wire is injected. The melt pool follows a pattern to fill each layer, progressively building the part. Several processes are included in this standard classification, such as laser powder forming, Laser Engineered Net Shaping (LENS), direct metal deposition, and laser consolidation. The resulting complex thermal history influences the microstructure, material properties, residual stress, and distortion of the final part. In an effort to understand these phenomena, many researchers have used Finite Element Analysis (FEA) to model the DED process and study its effects on the part [18,20,22,27,37,42,67,76–80,87–90].

FEA modeling of DED is inspired by weld modeling, since it is a similar process that has been studied extensively [3–5]. Although many of the weld modeling studies are directly applicable to DED modeling efforts, the convection models used are not applicable. Some weld studies have achieved useful results by neglecting convection while others have applied free convection uniformly on all exposed surfaces. These approaches lead to small errors in weld modeling because of the small amount of filler material relative to the substrate, which allows most of the heat to be conducted away from the bead into the parts being joined. In contrast, filler material makes up the majority of a
part built using DED, resulting in longer processing times and higher temperatures that allow for a greater amount of heat loss through convection. Consequently, greater errors can occur from inaccurate convection models in DED simulations. Complex convection models are required because of the inert gas jets often used to protect the laser optics, to shield the molten material from oxidation, and to aid in delivering powder to the melt pool. The heat transfer literature demonstrates that these types of jets create localized forced convection that is influenced by a variety of factors [44, 45, 62].

The literature shows inconsistent implementation of convection in DED models. Heat loss due to convection is assumed negligible and excluded in some models [32–35]. Convection is incorporated in other models by assuming it is uniformly distributed over all surfaces and equal to free convection in air (≃10 W/m²/°C) [21, 27, 37–43] while others have applied a higher uniform convection [64, 65], presumably to account for the greater amount of surface convection caused by the inert gas jets. Some researchers have considered the complexity of forced convection when modeling DED. Ghosh and Choi used the empirical equation defined by Gardon and Cobonque [101] to account for the forced convection [66]. Zekovic and co-workers included forced convection when modeling a thin wall deposition by using Computational Fluid Dynamics (CFD) to calculate the convection acting on the surface [67]. However, there was no experimental effort to validate the CFD results for their process. Furthermore, no work has been found in the literature that develops a measurement-based forced convection model.

This work proposes that measurement-based convection is a necessary component in an accurate model of the DED process. A thermo-mechanical model of DED of Ti-6Al-4V is developed that implements measurements of the convection generated
during the deposition process. The model is validated using *in situ* temperature and deflection measurements, as well as post-process measurements of the residual stress of three different depositions with varying geometry and dwell times. An additional convection model that assumes free convection is developed to illustrate the importance of implementing forced convection in the thermo-mechanical model.

### 4.3 DED Simulation

#### 4.3.1 Thermal Model

The DED process is simulated by first solving the thermal history of the process using a three dimensional transient thermal analysis [22]. The governing heat transfer energy balance is written as:

\[
\rho C_p \frac{dT}{dt} = -\nabla \cdot q(r, t) + Q(r, t)
\]  

(4.1)

where \( \rho \) is the material density, \( C_p \) is the specific heat capacity, \( T \) is the temperature, \( t \) is the time, \( Q \) is the heat source, \( r \) is the relative reference coordinate, and \( q \) is the heat flux vector, calculated as:

\[
q = -k\nabla T
\]  

(4.2)

where \( k \) is the thermal conductivity of the material.

Table 4.1 presents the temperature dependent thermal properties for Ti-6Al-4V [112]. Linear interpolation is used to calculate the properties at temperatures between those in the tables. At temperatures above 870 °C, the thermal properties are assumed to
be constant. The density of Ti-6Al-4V ($4.43 \times 10^3$ kg/m$^3$) is assumed to be independent of temperature. The latent heat of fusion is 365 kJ/kg and is spread over a temperature range from 1600 °C to 1670 °C [36].

Table 4.1. Temperature dependent thermal and mechanical properties of Ti-6Al-4V [89,112].

<table>
<thead>
<tr>
<th>$T$ (°C)</th>
<th>$k$ (W/m/°C)</th>
<th>$C_p$ (J/kg/°C)</th>
<th>$E$ (GPa)</th>
<th>$\sigma_y$ (MPa)</th>
<th>$\alpha$ (μm/m/°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>20</td>
<td>6.6</td>
<td>565</td>
<td>103.95</td>
<td>768.15</td>
<td>8.64</td>
</tr>
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<td>93</td>
<td>7.3</td>
<td>565</td>
<td>100.10</td>
<td>735.30</td>
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</tr>
<tr>
<td>205</td>
<td>9.1</td>
<td>574</td>
<td>94.19</td>
<td>684.90</td>
<td>9.09</td>
</tr>
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<td>91.81</td>
<td>664.65</td>
<td>9.20</td>
</tr>
<tr>
<td>315</td>
<td>10.6</td>
<td>603</td>
<td>88.38</td>
<td>635.40</td>
<td>9.33</td>
</tr>
<tr>
<td>425</td>
<td>12.6</td>
<td>649</td>
<td>82.58</td>
<td>585.90</td>
<td>9.55</td>
</tr>
<tr>
<td>500</td>
<td>13.9</td>
<td>682</td>
<td>78.63</td>
<td>552.15</td>
<td>9.70</td>
</tr>
<tr>
<td>540</td>
<td>14.6</td>
<td>699</td>
<td>76.52</td>
<td>534.15</td>
<td>9.70</td>
</tr>
<tr>
<td>650</td>
<td>17.5</td>
<td>770</td>
<td>70.72</td>
<td>484.65</td>
<td>9.70</td>
</tr>
<tr>
<td>760</td>
<td>17.5</td>
<td>858</td>
<td>64.91</td>
<td>435.15</td>
<td>9.70</td>
</tr>
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<td>17.5</td>
<td>895</td>
<td>62.80</td>
<td>417.15</td>
<td>9.70</td>
</tr>
<tr>
<td>870</td>
<td>17.5</td>
<td>959</td>
<td>62.80</td>
<td>417.15</td>
<td>9.70</td>
</tr>
</tbody>
</table>

The double ellipsoid model is used to describe the laser heat source [8]:

$$Q = \frac{6\sqrt{3}P\eta f}{abc\pi\sqrt{\pi}} e^{-\left[\frac{3x^2}{a^2} + \frac{3y^2}{b^2} + \frac{3(z+v)}{c^2}\right]}$$

(4.3)

The laser power is $P$ and the laser absorption efficiency is $\eta$. The value for $P$ is based on measurement, as will be discussed in Section 4.4. The value of $\eta$ is found using the method of inverse simulation described in [22] to be 45%. The variables $x$, $y$, and $z$
are local coordinates while the remaining variables \((f, a, b, \text{ and } c)\) define the volume over which the heat source is distributed. The volume is defined such that the heat source is circular with a radius equal to half the deposition track width and applied to a depth of 0.9 \(mm\). This results in a melt pool depth to radius ratio of 0.6, which agrees with the range extracted from cross-sections made by Kummailil using similar processing conditions [113].

Surface heat loss occurs on all free surfaces of the model through convection, \(q_{\text{conv}}\), and radiation, \(q_{\text{rad}}\). The free surfaces of the evolving deposition mesh are included in the analysis. Radiation is defined by the Stefan-Boltzmann law:

\[
q_{\text{rad}} = \varepsilon \sigma (T_s^4 - T_\infty^4) \tag{4.4}
\]

where \(\varepsilon\) and \(\sigma\) are the surface emissivity and the Stefan-Boltzmann constant. Emissivity is temperature independent and equal to 0.54, as in [22]. The surface temperature and the ambient temperature are represented by \(T_s\) and \(T_\infty\), respectively. The surface heat loss due to convection is defined by

\[
q_{\text{conv}} = h(T_s - T_\infty) \tag{4.5}
\]

where \(h\) is the coefficient of convection.

4.3.2 Mechanical Model

The mechanical response to the thermal history is determined by performing a three dimensional quasi-static incremental analysis [22]. The stress equilibrium is
governed by the following equation:

\[ \nabla \cdot \sigma = 0 \]  \hspace{1cm} (4.6)

where \( \sigma \) is the stress. The mechanical constitutive law is defined by:

\[ \sigma = C \epsilon_e \]  \hspace{1cm} (4.7)

\[ \epsilon = \epsilon_e + \epsilon_p + \epsilon_T \]  \hspace{1cm} (4.8)

\( C \) is the fourth order material stiffness tensor. The total, elastic, plastic, and thermal strains are represented by \( \epsilon, \epsilon_e, \epsilon_p, \) and \( \epsilon_T \).

Table 4.1 presents the temperature dependent elastic modulus \( (E) \), the yield strength \( (\sigma_y) \), and the coefficient of thermal expansion \( (\alpha) \) [89,112]. The mechanical values are assumed to be constant above 800 °C. The Poisson’s ratio is assumed to be 0.34 and temperature independent. Perfect plasticity is assumed in the model.

Instantaneous annealing and creep are applied to any element when the average temperature of its Gauss points exceeds the stress relaxation temperature, \( T_{\text{relax}} = 690 \) °C. Each strain component in Equation (4.8) is set to zero once this criteria is met. Instantaneous stress relaxation at this temperature has been shown by Denlinger and his co-workers to be necessary when modeling the deposition of Ti-6Al-4V [22].
4.4 Calibration and Validation Depositions

Single track thin walls of Ti-6Al-4V are deposited using an Optomec® LENS MR-7 system with a 500 W IPG Photonics fiber laser. The deposition occurs in a chamber with an argon atmosphere that has an oxygen content of less than 15 parts per million. A 30 L/min argon jet is used to supply argon to the chamber, to protect the laser optics, and to shield the melt pool. The Ti-6Al-4V powder delivered to the melt pool is assisted by four argon jets that have a combined flow rate of 4 L/min. These four jets exit nozzles positioned around the main nozzle and aimed at the melt pool, as shown in Figure 4.1. The powder has been sieved so that only particles with diameters between 44–149 μm are delivered at a rate of 3.0 g/min.

Fig. 4.1. An illustration of the LENS deposition head and powder nozzles.
The model is validated using three different depositions, as shown in Figure 4.2. Each case builds a wall that is designed to be 38.1 mm long, 12.7 mm tall, and 3 mm wide. These cases produce different thermal and mechanical results that are used to validate the model:

1. A single wall built using 62 layers, each one track wide, that are deposited without any dwell between layers onto a 76.2 mm long, 25.4 mm wide and 6.4 mm thick Ti-6Al-4V substrate.

2. A 2\textsuperscript{nd} 62 layer wall is deposited on top of the wall built in Case 1 without any dwell between each layer. This results in a final deposition that is a total of 124 layers, hereafter referred to as the double wall. This deposition increases the area over which the forced convection acts and allows a thermocouple to be attached to the wall to monitor its temperature.

3. A 62 layer wall is deposited onto a substrate with a 20 s dwell between each layer. This generates lower temperatures compared to the deposition with no dwell (Case 1).

Table 4.2 presents the process conditions used in each case. In all depositions the nominal power is 500 W; however, power measurements made using a Macken P500 power probe (with an accuracy of ± 25 W) before each deposition show that the actual power being supplied by the laser is between 410 W and 415 W.
Fig. 4.2. Images of the deposited thin walls.

Table 4.2. The test cases and process conditions used.

<table>
<thead>
<tr>
<th>Case</th>
<th>1</th>
<th>2</th>
<th>3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Measured laser power (W)</td>
<td>410</td>
<td>415</td>
<td>415</td>
</tr>
<tr>
<td>Travel speed (mm/s)</td>
<td>8.5</td>
<td>8.5</td>
<td>8.5</td>
</tr>
<tr>
<td>Powder delivery rate (g/min)</td>
<td>3.0</td>
<td>3.0</td>
<td>3.0</td>
</tr>
<tr>
<td>Additional dwell between layers (s)</td>
<td>0</td>
<td>0</td>
<td>20</td>
</tr>
<tr>
<td>Total wall height (mm)</td>
<td>11.2</td>
<td>23.2</td>
<td>10.7</td>
</tr>
<tr>
<td>Measured wall length (mm)</td>
<td>39.2</td>
<td>39.3</td>
<td>37.2</td>
</tr>
<tr>
<td>Measured wall width (mm)</td>
<td>3.0</td>
<td>3.1</td>
<td>2.2</td>
</tr>
</tbody>
</table>
4.4.1 Deposition Measurements

In situ deflection of the substrate is measured by clamping one of its ends into a fixture, cantilevering the free end over a Laser Displacement Sensor (LDS) Figure 4.3. The LDS used in this study is a Keyence LK-031, which has a measurement accuracy of ± 1 μm. It measures the vertical distance to a point on the bottom surface of the substrate, as shown in Figure 4.4(a).

![Fig. 4.3. The experimental setup.](image)

In situ temperature is measured at several locations on the substrate, as shown in Figure 4.4, using Omega GG-K-30 type K thermocouples. The thermocouples have a measurement uncertainty of 2.2 °C or 0.75%, whichever is larger. TC 1 is located on the bottom surface of the substrate, while TC 2 is on the top surface, near the wall. Aluminum foil tape was used to shield TC 2 from the effects of convection during Cases 2 and 3. It was not used during Case 1. Consequently, the TC 2 measurements from Case 1 could not be used because the gas flow affected the thermocouple measurements.
Fig. 4.4. The locations of each measurement.
Before the second wall is built in Case 2, an additional thermocouple (TC 3) is welded to the face of the existing wall, this thermocouple is not used in the other depositions.

The post-process residual stress is measured using the hole-drilling method at three points along the bottom surface of each substrate. Micro-Measurements® model EA-06-062RE-120 strain gauges, which have a measurement error of ± 50 MPa, are bonded to the substrate at the locations shown in Figure 4.4(a). The procedures described in manufacturing engineering data sheet U059-07 and technical note 503 are used to calibrate each gauge. The ASTM E837 incremental drilling process is followed using a RS-200 Mill Guide and a Micro-Measurements® high speed drill. Carbide tipped Type II Class 4A drill bits, with diameters of 1.52 mm are used to drill each hole to a final depth of 2 mm, in 0.25 mm deep increments. The strain measurements are made using a Micro-Measurements® P-3500 Strain Indicator and the gauge bridges are balanced with a Micro-Measurements® model SB-1 Switch and Balance Unit.

4.4.2 Convection Measurements

Hot-film constant voltage anemometry is used to measure the distribution of the surface convection generated by the deposition head [114]. The measurements are made using three Senflex® SF9902 single-element hot-film sensors that are adhered to the surface of a 3.2 mm thick plexiglass plate using MACfilm® IF-2012 adhesive. A constant voltage is supplied to each sensor using a Model 4-600 constant voltage anemometer (CVA). Both the sensors and CVA are from Tao of Systems Integration, Inc.

Two sensors are adhered onto a plexiglass plate that is mounted vertically to simulate the thin-wall geometry. Sensor 1 (S1) is mounted 0.8 mm below the top edge
of the plate, and Sensor 2 (S2) is mounted 5.4 mm below the top edge, as shown in Figure 4.5(a). The back-side of the plexiglass plate is ground so that the top edge has a 45° angle to minimize its effect on the argon jet. The measurements are made by positioning the bottom of the powder delivery nozzles 9.3 mm above the top edge of the plate and moving the head in 1 mm increments along the X axes. The 9.3 mm offset is also used when depositing material. The heat transfer coefficient, $h$, is calculated from the measurement data at each increment once steady state is achieved.

The heat transfer acting on a horizontal surface is measured by mounting a third sensor onto the center of the plexiglass substrate, as shown in Figure 4.5(b). Measurements are made by moving the deposition head in 3 mm increments along the positive and negative X and Y axes, which are centered at the sensor.

## 4.5 Numerical Implementation

### 4.5.1 The FEA Solver

The FEA analysis is performed using CUBIC (Pan Computing LLC), a Newton-Raphson based solver developed specifically to model additive manufacturing technologies. The hybrid “quiet”/inactive element activation method is used to simulate the deposition of material during the DED process [36]. The model initially includes all the elements in the substrate. Before each layer is deposited, its elements are introduced into the set of equations. When the elements of a layer are first introduced, they are “quiet,” that is their material properties are scaled to be smaller so that they do not affect the analysis before they are activated. Thermal conductivity ($k$), specific heat capacity
Fig. 4.5. The setups used to measure the forced convection.
and elastic modulus \((E)\) are scaled by \(10^{-6}\), \(10^{-2}\), and \(10^{-4}\), respectively. The properties of an element are switched from "quiet" to active when any Gauss point of the element is consumed by the heat source volume (Equation (4.3)). In addition to the properties being switched, the temperature of the activated element is reset to the ambient temperature to prevent erroneous heating of the element. The free surfaces of the part are re-assessed whenever an element is switch from "quiet" to active to ensure that convection and radiation are applied properly to the evolving part surface, which includes the interfaces between the "quiet" and active elements.

### 4.5.2 Finite Element Mesh

Figure 4.6 presents the 3-dimensional half-symmetry mesh that is used for both the thermal and mechanical analysis. The black surface represents the symmetry plane. The mesh comprises 23295 nodes and 15627 elements. There are 11904 elements in the 25.2 mm tall, 38.1 mm long, 1.5 mm thick wall. The aluminum clamp used to hold the substrate in the measurement fixture is included in the mesh to account for the heat transfer from the substrate into it. Each deposition layer is 1 element tall (0.203 mm) and 2 elements wide, equating to each element being equal to 1/4 of the laser diameter. The model is mechanically constrained such that it is cantilevered and allowed to deform in the same manner as the experiments.

Errors between measurement and simulation results are calculated by comparing single instance in time, or by calculating the percent error over the deposition time:

\[
\text{% Error} = \frac{100 \sum_{i=1}^{n} \left| \frac{T_{\text{meas},i} - T_{\text{node},i}}{T_{\text{meas},i}} \right|}{n}
\] (4.9)
Fig. 4.6. The half-symmetry finite element mesh of the substrate, wall, and the fixture’s aluminum clamp.
where \( n \) is the total number of simulated time increments between the beginning and end of the deposition, \( i \) is the current time increment, \( T_{\text{node}} \) is the simulated temperature, and \( T_{\text{meas}} \) is the measured temperature.

### 4.5.3 Convection Model

Figure 4.7 presents the results of the surface convection measurements used to develop the model. The measured convection acting on a vertical wall using the two sensors (S1 and S2) is presented in Figure 4.7(a). Each point in the plot is the mean value of four measurements and the error bars represent the standard deviation of each measurement. The convection acting on a horizontal surface, measured using S3, is presented in Figure 4.7(b). In this case, each point is the mean of eight measurements.

When measured on both a horizontal surface and a vertical wall, the value of \( h \) dissipates as the distance from the centerline of the jet increases. An exponential decay function is fit to the measurement data:

\[
h = h_a e^{-\left(\theta r\right)^\phi} + h_o \tag{4.10}
\]

where \( r \) is the distance from the centerline of the argon jet to the point of interest and \( h_o \) is the value measured at the outermost locations that the function decays to. The peak of the forced convection is defined by \( h_a \). The variables \( \theta \) and \( \phi \) are used to define the shape of the decay. Different values for these variables are required for the wall and for the substrate because the convection on the wall has a lesser magnitude and narrower distribution than on the horizontal surface.
Fig. 4.7. The results of the convection measurements.
4.5.3.1 Forced convection on a vertical wall:

The best fit to the wall convection data that is presented in Figure 4.7(a) is achieved when $\theta = 0.107$, $\phi = 2.7$ and $h_{o,\text{wall}} = 25 \text{ W/m}^2/\text{°C}$. The only variable that changes between the fit of the two data sets is $h_{a,\text{wall}}$, which decreases from a value of 35 W/m$^2$/°C at a distance of 0.8 mm (S1) from the top edge to a value of 23 W/m$^2$/°C at a distance of 5.4 mm (S2).

The decrease of $h_{a,\text{wall}}$ as the distance from the top edge of the wall increases is assumed to be linear:

$$h_{a,\text{wall}} = -2.717z + 37.174$$ (4.11)

where $z$ is the distance from the top edge of the wall to the point of interest. Substituting Equation (4.11) in Equation (4.10) yields the following equation that describes the convection on a thin, single track wide deposited wall:

$$h_{\text{wall}} = (-2.717z + 37.174)e^{-(0.107r)^{2.7}} + 25$$ (4.12)

This equation is only applicable up to a vertical distance of 13.7 mm from the top edge of the wall. At greater distances the effect of the jet is assumed to diminish so that $h_{a,\text{wall}} = 0 \text{ W/m}^2/\text{°C}$ and $h_{o,\text{wall}}$ remains 25 W/m$^2$/°C.

4.5.3.2 Forced convection on a horizontal surface:

The best fit to the horizontal surface convection data that is presented in Figure 4.7(b) is described when $\theta = 0.031$, $\phi = 1.4$, $h_{a,\text{surface}} = 70 \text{ W/m}^2/\text{°C}$, and
\( h_{o, \text{surface}} = 30 \, \text{W/m}^2/\degree\text{C} \). This is only applicable when the head is positioned so that it is depositing directly onto the horizontal surface and not when a wall is being deposited. To account for the deposition of the wall, it is assumed that \( h_{a, \text{surface}} \) decreases as the distance between the surface and the nozzle increases. Specifically, \( h_{a, \text{surface}} \) is directly related to \( h_{a, \text{wall}} \):

\[
h_{a, \text{surface}} = \frac{70}{37.174} h_{a, \text{wall}} = 1.9(-2.717z + 37.174) \quad (4.13)
\]

As a result, the model to define the forced convection acting on a horizontal surface as a wall is deposited on top of it is:

\[
h_{\text{surface}} = 1.9(-2.717z + 37.174)e^{-0.031r}^{1.4} + 30 \quad (4.14)
\]

It is assumed that once the effect of the jet on the wall has been fully dissipated, the localized forced convection defined by \( h_{a, \text{surface}} \) can no longer be generated on the surface. Consequently, \( h_{a, \text{surface}} = 0 \, \text{W/m}^2/\degree\text{C} \) when \( h_{a, \text{wall}} = 0 \, \text{W/m}^2/\degree\text{C} \).

4.5.3.3 Convection on the underside of the substrate:

There is no measurement data of the surface convection that occurs on the underside of the substrate during the deposition. It is assumed that it is equal to \( h_{o, \text{surface}} \).
4.5.3.4 Free convection in the absence of the argon jets:

After each deposition, the argon gas jets are shut off and no forced convection occurs. In addition, the argon atmosphere in the deposition chamber is no longer agitated by the argon flow. As a consequence, the convection becomes uniform on all surfaces and equal to free convection (10 W/m$^2$/°C).

4.6 Simulation Cases

Table 4.3 presents the convection models used to simulate each case to illustrate the impact of the convection model on the simulation results. The forced convection model is developed from measurements of the distribution of $h$. This model is independent of the deposition material. The free convection model assumes a uniform coefficient of convection on all surfaces equal to 10 W/m$^2$/°C. This value is approximately equal to the free convection in air used in other studies [21, 27, 37–43]. Figure 4.8 presents the surface convection from each model acting on a single wall deposition (Case 1 or 3) when it is half complete.

Table 4.3. The convection models used to simulate each case.

<table>
<thead>
<tr>
<th>Convection Model</th>
<th>$h_{a,\text{wall}}$</th>
<th>$h_{o,\text{wall}}$</th>
<th>$h_{a,\text{substrate}}$</th>
<th>$h_{o,\text{substrate}}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Forced</td>
<td>-2.717 $z + 37.174$</td>
<td>25</td>
<td>1.9 $h_{a,\text{wall}}$</td>
<td>30</td>
</tr>
<tr>
<td>Free</td>
<td>0</td>
<td>10</td>
<td>0</td>
<td>10</td>
</tr>
</tbody>
</table>
Fig. 4.8. The forced convection acting on a single wall when the wall is half complete. The convection scale is in $\text{W/m}^2/\degree\text{C}$. 
4.7 Results and Discussion

4.7.1 Thermal History

Figure 4.9 presents the simulated temperature distribution at the middle of each deposition using the forced convection model. The deposition of a single wall with no dwell between layers (Case 1) generates the highest temperature, since the heat is input quickly and the mass of the deposition is relatively small. The deposition of a second wall with no dwell (Case 2) experiences high temperatures in the wall, but lower temperatures in the substrate compared to the first wall in Case 1. This is due to the increased wall height that adds thermal mass to the part and also resists conduction into the substrate. Additionally, the increased wall height provides a greater surface area for the convection to extract heat from the part. The single wall deposition with a 20 s dwell between each layer (Case 3) experiences the lowest temperatures because the dwell allows for more cooling of the part.

Figure 4.10 presents the thermal measurements and simulation results of the single wall deposition using no dwell between layers (Case 1). The measurements made using TC 1, which is located on the center of the bottom surface of the substrate, are presented along with the corresponding simulation results. The measurement-based forced convection model results in the best thermal simulation results during the deposition process (0 s – 287 s), with a percent error of 0.8%, whereas the free convection model results in a percent error of 15.4%. It is clear from these results that the free convection model does not allow enough heat transfer and thus the temperatures are too
Fig. 4.9. The simulated temperature distribution within each deposition when it is half complete resulting from the measurement-based forced convection model. The temperature scale is in °C.
high. The measurement-based forced convection model results in greater heat transfer that produce more accurate thermal results.

Fig. 4.10. The temperature history of the single wall deposition with no dwell (Case 1). Measurements are made using TC 1, which is located on the bottom surface of the substrate, and compared to the simulation results. The dashed vertical line indicates the deposition conclusion.

Figure 4.11(a) presents the thermal measurements and simulation results of the deposition of the second wall using no dwell between layers (Case 2). As the surface area increases from the single wall (Case 1) to the double wall (Case 2), the error from each convection model increases. The error from the free convection model increases from 15.4% (Case 1) to 22.2% (Case 2), indicating that as the part size increases, the assumption of free convection leads to greater simulation error. On the other hand, the error from the forced convection model also increases, though by a lesser extent, from 0.8% (Case 1) to 4.2% (Case 2). This indicates that although the forced convection model is more accurate than the free convection model, it can be improved.
Fig. 4.11. The temperature history during the deposition of the second wall with no dwell between layers (Case 2). The dashed vertical line indicates the deposition conclusion.
Figure 4.11(b) compares the simulation results to the measurements made on the wall using TC 3. It was not possible to shield the thermocouple from the effect of convection because the thermocouple was welded on the wall close to the deposition. Consequently, the convection cools the thermocouple junction, making it experience lower temperatures while the jets are flowing. The temperature measurement rises quickly when the jets are shut off at approximately 293 s. Despite this difference during the depositions comparisons can be made at a time of 297 s, which is after the point that the argon gas supply was shut off. The forced convection produces a percent error of 5.7% at this time, while the percent error resulting from the free convection model is 19.5%. This further demonstrates the improved results using the measurement-based forced convection model.

The greatest errors between the measurements and simulations results occur during the single wall deposition with a 20 s dwell between each layer (Case 3), as shown in Figure 4.12. The measurement-based convection model results in a 4.9% error, while the free convection model produces a percent error of 43.8%. The increased processing time during this case allows for a greater amount of heat to be evacuated through convection.

It has been demonstrated in each case that the measurement-based forced convection produces superior results compared to the assumption of free convection; however, the forced convection model can be improved. This is understandable since it is developed from the measurements made at three locations on two different surfaces. One approach to develop an improved model is to perform more detailed measurements to investigate the effect of specific part geometries on convection. Another
Fig. 4.12. The temperature history of the single wall deposition with a 20 s dwell between layers (Case 3). Measurements are made using TC 2, which is located on the top surface of the substrate, and compared to the simulation results. The dashed vertical line indicates the deposition conclusion.

The approach would be to use convection measurements to validate CFD of the gas flow over the evolving deposition surface. The CFD approach would enable a broader range of deposition geometries to be model without necessitating further convection measurement. Whichever approach is used, convection measurements of the specific deposition equipment must be performed.

### 4.7.2 Deflection History

Figure 4.13 shows the final simulated deformation of the single wall deposition with no dwell between layers (Case 1). The distortion has been scaled up by a factor of 5 so that the deformation is evident. The deposition process bends the part upwards and shrinks the wall from its nominal size. Each case exhibits this behavior.

Figure 4.14 presents the measured and simulated deflection for the single wall with no dwell between layers (Case 1). The LDS deflection measurement is more sensitive to the strain of the material nearer to the clamp as a result of the substrate being...
cantilevered over it. When the laser is nearest the clamp the thermal expansion of the material near the melt pool causes the substrate to deflect downward. This occurs at the end of the even numbered layers and the beginning of the odd numbered layers. As the laser moves away from the the clamp, that material contracts as it cools, causing the substrate to deflect up, producing a consistent net increase in deflection during the deposition of layers 1–14 (0–62 s). The net deflection decreases during the deposition of layers 15–35 (62–155 s). The remaining layers cause a net increase in deflection, though the amplitude of the oscillation decreases as the wall height increases. Once the deposition concludes, indicated by the vertical dashed line at 287 s, the deflection increases rapidly until it reaches a steady state.

The measurement-based convection model produces the best correlation with the experimental measurements in the single wall deposition (Case 1). The lower
Fig. 4.14. A comparison of the simulated and measured deflection history of the single wall deposition with no dwell between layers (Case 1). The dashed vertical line indicates the deposition conclusion.
predicted deflection generated from the free convection model is a consequence of the
greater amount of instantaneous annealing at the higher temperatures. These results
demonstrate that a measurement-based convection model is required to produce thermal
and deflection results when instantaneous annealing occurs.

Figure 4.15 presents the measured and simulated deflection for the double wall
deposition with no dwell between layers (Case 2). The residual deflection in the part
from the deposition of the first wall in Case 1 has been subtracted from the results. The
deflection measured during the deposition of the wall is very different from that measured
during the deposition of the first in Case 1. The measured deflection decreases rapidly
as the wall experiences thermal expansion during the first several deposition layers, then
increases after the sixth track (at 32 s). Both of the simulation cases capture this trend
very well. The free convection model produces more accurate results compared to the
measurement-based forced convection model despite the inferior thermal results. This
indicates that the material properties are not fully captured in the mechanical model.
One possible cause for this is that the instantaneous annealing is actually time dependent,
as speculated by Denlinger et al. [115] but is not accounted for in this model.

Figure 4.16 presents the measured and simulated deflection during the deposition
of the single wall with a 20 s dwell between each layer (Case 3). The deflection
oscillates with each deposition layer and experiences a net increase after each layer.
Both simulation cases capture this trend. The measurement-based forced convection
model results in an over-prediction of the deflection, whereas the free convection model
under-predicts the deflection. However, the absolute difference between each model and
the experiments is approximately equal.
Fig. 4.15. A comparison of the simulated and measured deflection history of the double wall deposition with no dwell between layers (Case 2). The dashed vertical line indicates the deposition conclusion.
Fig. 4.16. A comparison of the simulated and measured deflection history of the single wall deposition with a 20 s dwell between layers (Case 3). The dashed vertical line indicates the deposition conclusion.


4.7.3 Residual Stress

Figure 4.17 presents the residual stress in the bottom surface of the substrate after each deposition has been allowed to cool. The error bars represent the measurement accuracy of the strain gages, which is ± 50 MPa. For comparison, the simulated stress from each case is extracted from the nodes along the centerline. The measurements and each simulation case exhibit a trend where the greatest stress occurs under the center of the wall. The stress decreases as the ends of the wall are approached. The wall edges are indicated by the dashed vertical lines. In each case, the measurement-based forced convection model produces the highest stresses compared to the free convection model and the best results compared to the measurements.

4.8 Conclusions

Experimentally measured surface convection is implemented into a thermo-mechanical model of DED additive manufacturing. Three different thin-wall cases, with different geometries and dwell times, are made to validate the thermo-mechanical model. To illustrate the need for the measurement-based forced convection model, a second model is developed that assumes free convection on all surfaces, which is a common approach used in the literature.

Comparisons between in situ temperature measurements simulation results show that the measurement-based forced convection model achieves the most accurate results for each case, with percent errors of less than 5% when compared to the measurements, whereas the free convection model simulates temperatures with percent errors of up to
Fig. 4.17. The residual stress measurements and simulation results. The dashed vertical line indicates the edges of the wall.
44% for the three depositions. Although the measurement-based forced convection model produces superior results, it can be improved through more accurate geometry specific measurements, or through validated CFD analysis.

The residual stress measurements and in situ deflection measurements show that the measurement-based convection model produces more accurate stress measurements in all cases. However, the forced convection model produces more accurate deflection in only one of the cases, despite the superior thermal results, indicating that a more detailed mechanical model is required.

4.9 Acknowledgments

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Chapter 5

The Impact of Argon Shielding Flow Rate on Laser Engineered Net Shaping (LENS) of Ti-6Al-4V

5.1 Abstract

The impact of the surface convection generated by argon shielding gas used during Laser Engineered Net Shaping (LENS) on the deposited part is investigated. LENS generates complex thermal histories in the build that are dependent upon the material, part geometry, laser heat input, mass feed rate, and radiative as well as convective cooling. It has been shown that the deposition track and final part quality are subject to many of these considerations; however, no investigation has been undertaken to understand the impact of surface convection resulting from changing the argon shielding gas flow rate. A thermo-mechanical finite element analysis with measurement-based convection models is used to study the effects from different argon shielding flow rates on the temperature history, distortion, and residual stress of thin wall Ti-6Al-4V structures. The results from three cases show that reducing the shielding flow rate by half increases the temperature by 5% to 13%, while decreasing the deflection and residual stress.

1 The work presented in this chapter has been accepted through peer review: Heigel, J.C., Michaleris, P. “The Impact of Argon Shielding Flow Rate on Laser Engineered Net Shaping (LENS) of Ti-6Al-4V.” Proceedings of the 2015 ASME International Conference on Manufacturing Science and Engineering.
5.2 Introduction

Additive Manufacturing (AM) is a process used to build parts layer-by-layer directly from digital files. Directed Energy Deposition (DED) is a subset of AM in which material is injected directly into a melt pool generated by a high intensity heat source traversing across each cross-sectional layer [111]. Some DED systems, such as Laser Engineered Net Shaping (LENS) [116], use a laser to create the melt pool into which powder is injected. The complex thermal history resulting from DED influence the microstructure and material properties [117–121], as well as the residual stress [23, 27, 41, 42, 73, 75] and distortion [21, 90, 92] of the final part. Because these outcomes are highly dependent on the processes parameters, many researchers have investigated the influence of each parameter on the formation of the deposition track and on the quality of the final part.

Formation of the deposition track is modeled using a mass-energy balance [19, 77, 96, 100, 122–124]. The energy in this analysis is dependent upon the laser power and its distribution, as well as the heat loss through conduction into the bulk material or radiation and convection from the free surfaces. The masses involved are the substrate, part, and powder being injected into the melt pool. In order to create an accurate model of the process, some researchers have measured the concentration of the powder in the inert gas carrier jets [96, 125, 126] while others have used computational fluid dynamics (CFD) and other analysis techniques to model the inert gas jets and the concentration within them [124, 125, 127–131]. Accurate modeling of the track formation is useful;
however, the computation expense of this type of analysis prohibits it from modeling the
deposition of an entire part.

Finite element analysis (FEA) is a capable tool for analyzing entire parts [80, 92, 132] because the heat source and mass addition are approximated, making the analysis possible. For example, the laser is often modeled as a volumetric heat source [8] while the mass addition is simulated through the activation of elements in the mesh [36]. The size of the elements is derived from measurements [80], numerical calculations of single deposition tracks [19, 124], or the nominal layer thickness and hatch spacing. Because these approximations capture the heat input and mass addition, they are useful in studying the effect of process parameters such as laser power [27, 41, 42, 71, 133], speed [41, 42, 71, 72], deposition pattern [65, 99], inter-layer dwell times [80], and material properties [71].

Accurate FEA simulations can only be achieved if the approximations of the process physics do not neglect important aspects that affect the energy balance. For instance, the powder concentration and ultimately the deposition rate are directly related to the inert gas jets; however, these jets are often neglected in FEA models [21, 27, 32–35, 37–43]. Recent work demonstrated that simulations using the common assumption of natural convection produces temperatures with errors 4 to 6 times larger than simulations which implement measurement-based force convection [134]. However, these improvements were achieved with forced convection that was several times larger than the assumed natural convection. It is unclear how sensitive the model is to changes in the jet flow rate.
This work investigates the effect of different shielding gas flow rates on the temperature and mechanical response of LENS deposited thin wall structures. An existing validated FEA model of LENS deposition of Ti-6Al-4V thin walls [134] is used to study the impact on convection on temperature, distortion, and residual stress. The measured surface convection generated by two different flow rates is implemented in the model, and three different deposition cases are investigated.

5.3 LENS Simulation

The thermo-mechanical analysis is executed by first solving the thermal analysis, then using the results as the thermal load in the mechanical analysis. The model has been validated for LENS of Ti-6Al-4V [134].

5.3.1 Thermal Model

The heat transfer energy balance that governs the 3D transient thermal analysis is:

\[ \rho C_p \frac{dT}{dt} = -\nabla \cdot \mathbf{q}(\mathbf{r}, t) + Q(\mathbf{r}, t) \]  

(5.1)

where \( \rho \) is the material density, \( C_p \) is the specific heat capacity, \( T \) is the temperature, \( t \) is the time, \( Q \) is the heat source, \( \mathbf{r} \) is the relative reference coordinate, and \( \mathbf{q} \) is the heat flux vector, calculated as:

\[ \mathbf{q} = -k \nabla T \]  

(5.2)

where \( k \) is the thermal conductivity of the material. Table 5.1 presents the temperature dependent thermal properties for Ti-6Al-4V [112]. The density equals \( 4.43 \times 10^3 \, \text{kg/m}^3 \).
and is assumed to be independent of temperature. The latent heat of fusion, spread over a temperature range from 1600°C to 1670°C, is 365 kJ/kg [36].

Radiation, \( q_{\text{rad}} \), and convection, \( q_{\text{conv}} \), extract heat from the free surfaces:

\[
q_{\text{rad}} = \varepsilon \beta (T_s^4 - T_\infty^4) \quad (5.3)
\]

\[
q_{\text{conv}} = h(T_s - T_\infty) \quad (5.4)
\]

where \( T_s \) and \( T_\infty \) are the temperature on the free surface and the ambient temperature, respectively. The surface emissivity and Stefan-Boltzman constant are represented by the variables \( \varepsilon \) and \( \beta \). Emissivity is temperature independent and equal to 0.54 [22,134]. The coefficient of convection is \( h \).

5.3.2 Mechanical Model

The mechanical response to the thermal history is determined by performing a 3D quasi-static incremental analysis, which is governed by stress equilibrium and the mechanical constitutive law:

\[
\nabla \cdot \sigma = 0 \quad (5.5)
\]

\[
\sigma = C \varepsilon_e \quad (5.6)
\]

\[
\varepsilon = \varepsilon_e + \varepsilon_p + \varepsilon_T \quad (5.7)
\]

where \( \sigma \) is the stress and \( C \) is the fourth order material stiffness tensor. The total, elastic, plastic, and thermal strains are represented by \( \varepsilon \), \( \varepsilon_e \), \( \varepsilon_p \), and \( \varepsilon_T \). Temperature
Table 5.1. Temperature dependent thermal and mechanical properties of Ti-6Al-4V [89, 112].

<table>
<thead>
<tr>
<th>$T \ (\degree C)$</th>
<th>$k \ (W/m/\degree C)$</th>
<th>$C \ (J/kg/\degree C)$</th>
<th>$E \ (GPa)$</th>
<th>$\sigma \ (MPa)$</th>
<th>$\alpha \ (m/m/\degree C)$</th>
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<td>959</td>
<td>62.80</td>
<td>417.15</td>
<td>9.70</td>
</tr>
</tbody>
</table>
dependent elastic modulus ($E$), yield strength ($\sigma_y$), and coefficient of thermal expansion ($\alpha$) are presented in Table 5.1 [89,112]. At temperatures above 800 $^\circ$C, these properties are assumed to be constant. The Poissons’s ratio is assumed to be temperature independent and equal to 0.34. The assumption of perfect plasticity is implemented in the model. Instantaneous annealing and creep occur at temperatures above the relaxation temperature, $T_{\text{relax}} = 690$ $^\circ$C, which was found to be necessary when modeling Ti-6Al-4V [22].

5.4 Numerical Implementation

5.4.1 The FEA Solver

The FEA analysis is performed using Cubic (Pan Computing LLC) which is a Newton-Raphson based solver for modeling AM processes. Material deposition is simulated using the hybrid quiet/inactive element activation method [36]. The equation set initially excludes the elements in the deposition. Before the deposition of each layer, the corresponding elements are introduced into the set of equations as quiet, that is their material properties are scaled down so that they do not affect the analysis before they are activated. Thermal conductivity, specific heat capacity, and elastic modulus are scaled by $10^{-6}$, $10^{-2}$, and $10^{-4}$, respectively. As the laser travels across the layer, it activates elements when any of their Gauss points are contacted by the volume defined by the heat source model [8,134]. These elements become active by resetting their temperature to the ambient and removing their scaling factors. In addition, the free surfaces are re-assessed.
when elements are activated to ensure that radiation and convection are applied to the free surfaces present at each time increment.

5.4.2 Finite Element Mesh

A 3D half-symmetry mesh used for both the thermal and mechanical analysis, as shown in Figure 5.1. The mesh comprises 23295 nodes and 15627 elements. The 25.2 mm tall, 38.1 mm long, 1.5 mm thick half symmetry wall contains 11904 of the elements, with each element as thick as the deposited layer. The width and length of these elements is equal to 1/2 of the laser radius. The aluminum clamp used to hold the substrate in the measurement fixture is included in the mesh to account for the heat transfer from the substrate into it. The model is mechanically constrained so that it is cantilevered and allowed to deform in the same manner as the experiments.

Fig. 5.1. The half-symmetry finite element mesh of the substrate, wall, and the fixture’s aluminum clamp.
Nodes A and C are used to investigate the temperature and deflection history, respectively. Line A-A’ is used to investigate the temperature gradient through the wall and substrate. Residual stress is extracted from the nodes on Line B-B’. Errors between simulation results are calculated by comparing single instance in time, or by calculating the percent error over the deposition time:

\[
\% \text{Error} = \frac{100 \sum_{i=1}^{n} \left| \frac{T_{\text{sim2},i} - T_{\text{sim1},i}}{T_{\text{sim2},i}} \right|}{n}
\]

(5.8)

where \(n\) is the total number of simulated time increments between the beginning and end of the deposition, \(i\) is the current time increment, \(T_{\text{sim1}}\) is the temperature resulting from one simulation, and \(T_{\text{sim2}}\) is the temperature from the other simulation.

### 5.4.3 Convection Models

Two measurement-based convection models are used to account for the effects from different shielding flow rates using the method described by Heigel et al. [114]. Hot film sensors are mounted to a plexiglass substrate that is positioned either perpendicular to the flow or bisecting it. The energy balance in the nickel sensor element during constant voltage anemometry is used to calculate the heat transfer coefficient, \(h\), at each measurement location. Figure 5.2 presents the measured convection resulting from shield flow rates of 15 L/min and 30 L/min. In each measurement, the flow rate of the argon gas used to deliver powder to the melt pool from 4 nozzle surround the main coaxial nozzle is 4 L/min.
Fig. 5.2. The measured convection for shielding flow rates of 15 L/min and 30 L/min.
The convection model for the 30 L/min shielding flow rate was developed and validated experimentally by the authors in earlier work [134]. The distribution of the coefficient of convection (W/m$^2$/°C) on the wall, $h_{\text{wall}}$, is defined by:

$$h_{\text{wall}} = (-2.7z + 37.2)e^{-\left((0.107r)^{2.7}\right)} + 25 \quad (5.9)$$

where $z$ is the distance, in mm, from the top edge of the wall to the point of interest and $r$, in mm, is the distance from the centerline of the argon jet to the point of interest.

The distribution of the coefficient of convection resulting from the shielding gas impinging on the substrate, $h_{\text{surface}}$, is directly related to the wall height and is modeled using:

$$h_{\text{surface}} = 1.9(-2.7z + 37.2)e^{-\left((0.031r)^{1.4}\right)} + 30 \quad (5.10)$$

Values of $z$ greater than 13.7 mm result in a constant convection coefficient of $h_{\text{wall}} = 25$ W/m$^2$/°C and $h_{\text{wall}} = 30$ W/m$^2$/°C. This convection model was validated using thermal history of several Ti-6Al-4V thin wall depositions [134], similar to those in this study.

The convection model for the 15 L/min shield flow rate is developed from the appropriate convection measurements using the same method. The coefficient of convection on the wall is defined by:

$$h_{\text{wall}} = (-4.3z + 49.5)e^{-\left((0.13r)^{1.4}\right)} + 24 \quad (5.11)$$
and the coefficient of convection on the substrate surface is:

\[ h_{\text{surface}} = 2.2(-4.3z + 49.5)e^{-(0.005r)} + 21 \]  

(5.12)

Values of \( z \) greater than 11.4 mm result in a constant convection coefficient of \( h_{\text{wall}} = 24 \text{ W/m}^2/\circ\text{C} \) and \( h_{\text{wall}} = 21 \text{ W/m}^2/\circ\text{C} \). Due to the complex interaction of processing parameters, experiments only varying the shield flow rate could not be performed without potentially affecting the deposition quality.

Figure 5.3 presents the distribution of the convection coefficient, which is calculated from each model, acting on the single wall when it is half complete. It is apparent that reducing the shielding flow rate from 30 L/min to 15 L/min causes the forced convection to be more concentrated.

### 5.5 Simulation Cases

Table 5.2 presents the cases used to investigate the impact that changing the shielding flow rate has on the thermal history and mechanical response of three different depositions. Case 1 is a single wall built using 62 layers, each one track wide, that are deposited without any dwell between layers. Case 2 is a second 62 layer wall deposited without any dwell between each layer on top of the wall built in Case 1. The deposition in Case 1 is allowed to cool to the ambient temperatures before Case 2 begins. Case 3 is a 62 layer wall deposited with a 20 s dwell between each layer, resulting in lower temperatures than Case 1. Each of these deposition models using the 30 L/min convection model were experimentally validated [134].
Fig. 5.3. The forced convection resulting from the two different shielding flow rates acting on a wall when it is half complete.

<table>
<thead>
<tr>
<th>Case</th>
<th>1</th>
<th>2</th>
<th>3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Laser power (W)</td>
<td>410</td>
<td>415</td>
<td>415</td>
</tr>
<tr>
<td>Laser radius (mm)</td>
<td>1.5</td>
<td>1.5</td>
<td>1.5</td>
</tr>
<tr>
<td>Travel speed (mm/s)</td>
<td>8.5</td>
<td>8.5</td>
<td>8.5</td>
</tr>
<tr>
<td>Deposited layers</td>
<td>62</td>
<td>62</td>
<td>62</td>
</tr>
<tr>
<td>Dwell between layers (s)</td>
<td>0</td>
<td>0</td>
<td>20</td>
</tr>
<tr>
<td>Processing time (s)</td>
<td>287</td>
<td>287</td>
<td>1507</td>
</tr>
<tr>
<td>Total wall height (mm)</td>
<td>12.7</td>
<td>25.4</td>
<td>12.7</td>
</tr>
</tbody>
</table>
5.6 Results and Discussion

5.6.1 Thermal History

Figure 5.4(a) presents the results of the single wall deposition using no dwell between layers (Case 1) at Node A, which is located on center of the bottom surface of the substrate. Decreasing the shielding flow rate from 30 L/min to 15 L/min increases the temperature at Node A by 5.5% during the deposition. The maximum temperature difference of 36 °C occurs at the conclusion of the deposition (287 s). The temperature distribution along the vertical line (A-A’) through the middle of the part shows in Figure 5.4(b) presents the temperature profile along line A-A’ when the final layer is being deposited. A difference in the temperatures exist through the majority of the wall and substrate. However, near the top of the wall there is little difference in temperature.

Figure 5.5(a) presents the results of the deposition of the second wall using no dwell between layers (Case 2). The temperature during the deposition increases by 5.2%, with a maximum difference of 24 °C, when the shielding flow rate is decreased from 30 L/min to 15 L/min. Figure 5.5(b) shows that the temperature difference remains significant through most of the wall. Compared to the single wall (Case 1), decreasing the shielding flow rate causes less of a difference in temperature because the substrate experiences lower temperatures due to the greater mass and conduction resistance from the double wall.

Figure 5.6(a) presents the results of the deposition of the single wall with a 20 s dwell between layers (Case 3). The difference between the two cases at Node A during the deposition is 13.4%, with the maximum difference of 26 °C. The greatest percent
Fig. 5.4. The temperature results from the single wall deposition with no dwell (Case 1). a) presents the history at Node A, while b) presents the temperature distribution in Line A-A’ when the laser is at the mid-point of the final layer.
Fig. 5.5. The temperature results from the double wall deposition with no dwell (Case 2). a) presents the history at Node A, while b) presents the temperature distribution in Line A-A’ when the laser is at the mid-point of the final layer.
difference occurs during Case 3 because of the increased processing time that allows a greater amount of heat loss through convection.

5.6.2 Deflection History

Figure 5.7 presents the deflection at Node B for the single wall with no dwell between layers (Case 1). Throughout the process, the deflection resulting from the 15 L/min shielding flow rate is consistently less than the 30 L/min shielding flow rate. When the deposition concludes at 287 s, the deflection from the 15 L/min shielding flow rate is 0.011 mm less than the deflection resulting from the 30 L/min shielding flow rate. The difference increases to 0.020 mm after the part cools. The lower shielding flow rate produces less deflection because the higher temperatures (Figure 5.4) allow a greater amount of stress relaxation in the part, which has been shown to reduce deflection [115].

Figure 5.8 presents the deflection history at Node B for the double wall deposition with no dwell between layers (Case 2). There is little difference between the deflection from the two shield flow rates. For example, the greatest difference between the two models is only 0.004 mm, which occurs when the deposition concludes at 287 s.

The deflection during the 20 s dwell between each layer (Case 3) is presented in Figure 5.9. The 15 L/min shielding flow decreases the deflection by 6.9% during the deposition, and results in a final deflection that is 0.017 mm less. Comparing these results to those of the single wall deposition with no dwell (Case 1), the difference in the final deflection is less (0.017 mm vs. 0.020 mm) even though there is a greater temperature difference when using a 20 s dwell (13.4%) compared to no dwell (5.5%). The deflection is more sensitive to the difference in temperature resulting from the two
Fig. 5.6. The temperature results from the single wall deposition with 20 s dwells (Case 3). a) presents the history at Node A, while b) presents the temperature distribution in Line A-A’ when the laser is at the mid-point of the final layer.
Fig. 5.7. The deflection history of the single wall deposition with no dwell between layers (Case 1).

Fig. 5.8. The deflection history of the double wall deposition with no dwell between layers (Case 2).
convection models when no dwell is used (Case 1) because the higher temperatures result in a greater amount of material above the stress relaxation temperature which affects the accumulation of deflection.

5.6.3 Residual Stress

Figure 5.10 presents the residual stress along line B-B’ (Figure 5.1). In each case, the 15 L/min shielding flow rate decreases the stress compared to the 30 L/min flow rate because of the higher temperatures in the part. The single wall deposition in Case 1 experiences a maximum stress decrease of 7 MPa (8.8%), while both the double wall deposition (Case 2) and the single wall deposition with a 20 s dwell (Case 3) experience a maximum stress decrease of 8 MPa (8.6% and 4.3%, respectively). Both of the single wall cases (Cases 1 and 3) experience significant decreases in both stress and deflection, while the double wall deposition (Case 3) experiences a significant stress decrease but almost no deflection decrease.

5.7 Conclusions

A thermo-mechanical model is used to investigate the impact of decreasing the argon shielding flow rate during LENS deposition of Ti-6Al-4V. Measurement-based convection models are developed for two different shielding flow rates, 15 L/min and 30 L/min, and implemented into the model. Three different deposition cases, with different build geometries and dwell times, are simulated to investigate the impact of the different convection models on the thermal and mechanical history of the build.
Fig. 5.9. The deflection history of the single wall deposition with a 20 s dwell between layers (Case 3).
Fig. 5.10. The residual stress results.
The results show that the lower convection generated by the lower shielding flow rate increases the temperatures by 5% to 13%, and decreases the deflection and residual stress in the parts. However, the amount of this change is dependent upon the geometry and dwell time. For example, the larger geometry of the double wall deposition experiences the least change in temperature and deflection, while the longer processing time in the single wall deposition with a 20 s dwell between layers causes the greatest changes.

From these results, it is recommended that the effect from the inert gas jets used during LENS should be fully accounted for. While earlier work demonstrated that the forced convection from these jets must be accounted for to produce accurate results, this work illustrates that changes in the flow rate of the shielding jet must also be accounted for to achieve the most accurate results. Furthermore, it is recommended that during the deposition of Ti-6Al-4V the inert gas flow rates should be reduced to decrease the convection, which will cause the part temperature to increase and its deflection and residual stress to decrease.

5.8 Acknowledgments

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Chapter 6

Selection of Powder or Wire Feedstock Material for the Laser Cladding of Inconel® 625

6.1 Abstract

The selection of feedstock material significantly affects the temperature and resulting distortion generated during laser cladding. An investigation is undertaken using experiments to characterize the difference in temperature and deformation history resulting from laser cladding using either powder or wire. The in situ measurements show that the selection of a powder feedstock results in higher temperatures and greater deformation. Measurements of the clad reveal that both feedstock produce good quality clads, though the powder clad is is twice as thick as the wire clad. A thermo-mechanical model is developed to show that the disparity in laser absorption efficiency is responsible for the variation in temperature between the powder and wire. Simulations of a multi-layer deposition are performed to supplement the analysis. They show that although a single wire layer generates lower temperatures and less deformation than a single powder layer, the wire clad will actually produce greater total deformation because two layers are required to achieve the same thickness as the powder clad.
6.2 Introduction

Laser cladding is an additive manufacturing process in which a melt pool is created and traversed across a metallic substrate into which either powder or wire is injected to build-up thin layers. These clad layers are added to improve the properties and performance of a part, or to replace material that has been worn away [93]. Laser cladding is preferable compared to arc welding based processes because it results in lower levels of dilution and distortion [93]. Unfortunately, the the large thermal gradients and contraction of molten material generate residual stresses and distortion in the substrate that cannot be completely avoided.

Several studies have been undertaken to study the stresses and distortion generated during laser cladding with powder. For instance, Grum and Žnidarič used powder of several different materials to clad structural steel in order to improve its surface properties [17]. In situ measurements of the process using strain gauges and post process measurements of the residual stress revealed that the distortion and stress were affected by the clad pattern and the powder material. Ocelšk et al. clad steel and stainless steel substrates using a variety of powders and measured the strain during the clad process using digital image correlation techniques [91]. Their results indicated that substantial strain was generated due to local heating as the laser passed over the measurement point and that by increasing the laser speed, which decreased the linear heat input, this strain could be decreased. Heigel et al. performed an investigation into the impact of processing conditions on the magnitude and mode of distortion generated during laser cladding of Inconel® 625 powder onto substrates of the same material [99].
Thermocouples and a laser displacement sensor were used to measure the temperature and deflection of the substrates in situ. They found that the long processing times enabled a significant amount of heat transfer through convection and radiation which in turn impact the distortion. In fact, follow up work by Heigel et al. and Gouge et al. demonstrated that the forced convection generated by argon jets during processing must be accounted for in finite element (FE) models to accurately model the energy balance [134, 135]. Plati et al. measured the temperature and distortion of a stainless steel substrate clad with a nickel based metal matrix composite (MMC) powder [18]. They noted that the deposition of the powder had little impact on the distortion of the substrate due to the relatively thin coating and the majority of the laser energy being absorbed by the substrate, even though a portion of the energy is absorbed by the powder before it reaches the melt pool.

The ability of the powder to absorb energy from the laser beam before it reaches the melt pool has been the focus of many studies. Lin measured the temperature distribution of a stainless steel powder stream flowing coaxially through a 1 kW laser beam [136], showing that the powder can melt in-flight before it reaches the melt pool. However, its ability to do so is dependent upon the laser power and velocities of the particles and the argon gas carrier jet. Partes found it necessary to include the in-flight melting of a cobalt alloy powder to accurately model the capture efficiency [137]. The absorption of the powder in-flight can have measurable effects on the process temperatures during laser cladding. For example, Pinkerton and Li compared gas- and water-atomized powders and found that higher melt pool temperatures were achieved using the water-atomized 316L and H13 steel powders [138]. This difference was not
attributed to the size discrepancies of the powders, but instead to the increased oxidation and roughness of the water-atomised powder surfaces, which increase the surface coupling efficiency. Bi et al. investigated the impact of powder size on the process temperatures [139]. Using two powder lots with size distributions from 20 μm to 53 μm and from 53 μm to 150 μm, each delivered to the melt pool with the same mass rate of 2.4 g/min, they found that increasing the powder diameter had no effect on part temperatures despite the decreased number of particles in the powder stream.

An alternative to powder is wire feedstock. Wire is appealing because it is less expensive and results in less waste [93]. It can produce superior clad surfaces, reduce cost and contamination, and be used out of position [140,141]. Despite these advantages, care must be taken when laser cladding because it has been shown that the clad quality is sensitive to the direction and position of the wire fed into the melt pool [142]. In fact, wire should be fed directly into the melt pool so that it can be completely melted by the heat of the melt pool [143]. If not plunged into the melt pool, the wire can melt and form drops which fall onto the surface and degrade the clad quality.

Regardless of the feedstock, it has been shown that the melt pool depth is dependent on the amount of energy that reaches the substrate. For example, Kim and Peng found that when laser cladding single tracks of Inconel® 600 wire onto Inconel® 690 plates with a 220 W laser, the melt pool depth decreased from 0.5 mm to 0.25 mm when the travel speed was increased from 1.5 mm/s to 2.5 mm/s [144]. In general dilution and melt pool depth are directly related to laser power and inversely related to travel speed [93], and therefore directly related to the linear heat input. Similarly, Toyserkani et al. affected the melt pool depth not by changing the laser power or speed,
but by increasing the mass feed rate of the powder feedstock [145]. They found that increasing the powder feed rate decreased the temperatures of the substrate and the melt pool depth. This was attributed to the greater amount of absorbed laser energy in the powder which in turn decreased the amount of laser energy that could be absorbed by the substrate. In fact, when the mass feed rate became too great it prevented the formation of a melt pool and drastically decreased the bond strength between the clad and substrate.

No work has been found in the literature that investigates the impact that the choice between powder and wire feedstock has on the clad process. The objective of this study is to determine how the selection between powder and wire feedstock affects the temperature history, melt pool depth, deformation, and residual stress of the laser clad part. An experimental investigation is performed where all variables are held constant, except for the Inconel® 625 feedstock material which is either powder or wire in each case. Measurements of the temperature and deflection of the substrate are made using in situ techniques. In addition, cross sections are obtained from each clad to verify the clad quality and to investigate the melt pool dilution. In addition, a thermo-mechanical finite element (FE) model is developed to supplement the experimental investigation.

### 6.3 Experimental Procedure

Inconel® 625 plates, which are 152.4 mm long, 76.2 mm wide and 6.35 mm thick, are clad using a YLR-12000 IPG Photonics® fiber laser. The laser operates in the 1070-1080 nm wavelength range and is delivered through a 200 μm diameter fiber, then through a collimator with a 200 mm focal length, and finally through a lens with a
200 mm focal length. A Precitec YC50 clad head to shield the melt pool and deliver powder to it. Two different coaxial argon jets can be emitted from the clad head during processing. The first jet shields the melt pool and protects the laser optics while the second jet delivers powder to the melt pool. Each jet is supplied with argon at a flow rate of 9 L/min. The clad head is nominally positioned 10 mm above the part surface.

The process conditions used in this study are presented in Table 6.1. In each case, the nominal laser power \( P \) is 2.5 kW, the travel speed \( v \) is 10.6 mm/s, and the hatch spacing is 2.032 mm. The nominal linear heat input is the product of the nominal laser power and travel speed:

\[
\text{linear heat} = \frac{P}{v}
\]  

(6.1)

Each of the 36 passes, which are 109 mm long, are deposited in the same direction along the longitudinal axis of the plate. Inconel® 625 powder sieved to a particle size distribution between 44 \( \mu \)m and 149 \( \mu \)m (-100/+325 sieve size) is injected at a rate of 19 g/min into the melt pool to produce the powder clad. Only a percentage of this powder is added to the melt pool, as described by the capture efficiency:

\[
\text{capture efficiency} = \frac{m_f - m_i}{f \frac{lp}{v}}
\]  

(6.2)

where \( m_i \) and \( m_f \) are the weights of the plate before and after the cladding process, \( f \) is the powder feed rate, \( l \) is the pass length, \( p \) is the number of passes, and \( v \) is the laser velocity. Inconel® 625 wire with a diameter of 0.76 mm is injected at a rate of 20.32 mm/min into the melt pool, which equates to a mass rate of 9.22 g/min, to produce the wire clad. All of the wire material is consumed in the melt pool. The leading wire
feeder delivers the wire the melt pool at an angle of $20^\circ$ from the substrate. Both argon jets, without the inclusion of powder, are used to minimize the deviation in convective cooling between the two processes.

Table 6.1. The process conditions used in this study.

<table>
<thead>
<tr>
<th></th>
<th>Inconel® 625 feedstock</th>
<th>Powder</th>
<th>Wire</th>
</tr>
</thead>
<tbody>
<tr>
<td>Nominal mass feed rate (g/min)</td>
<td>19.0</td>
<td>9.22</td>
<td></td>
</tr>
<tr>
<td>Capture efficiency (%)</td>
<td>72</td>
<td>100</td>
<td></td>
</tr>
<tr>
<td>Nominal laser power (kW)</td>
<td>2.5</td>
<td>2.5</td>
<td></td>
</tr>
<tr>
<td>Travel speed (mm/s)</td>
<td>10.6</td>
<td>10.6</td>
<td></td>
</tr>
<tr>
<td>Nominal linear heat input (J/mm)</td>
<td>236</td>
<td>236</td>
<td></td>
</tr>
<tr>
<td>Number of passes</td>
<td>36</td>
<td>36</td>
<td></td>
</tr>
<tr>
<td>Hatch spacing (mm)</td>
<td>2.03</td>
<td>2.03</td>
<td></td>
</tr>
<tr>
<td>Pass length (mm)</td>
<td>109.2</td>
<td>109.2</td>
<td></td>
</tr>
<tr>
<td>Clad thickness (mm)</td>
<td>1.42</td>
<td>0.74</td>
<td></td>
</tr>
</tbody>
</table>

An offset distance of 10 mm between the bottom of the clad head and the part ensures the intended laser beam diameter, powder concentration, and injection of the wire into the melt pool. Because the cantilevered setup in this study allows the plate to freely deflect, the vertical positioning of the clad head must be programmed to maintain the offset and resulting clad quality. As each track is deposited, the head is moved upward to follow the anticipated deflection of the plate resulting from the contraction of the molten material. The deflection measured in [99] is used to anticipate the deflection of each track, as illustrated in Figure 6.1. This strategy produces clads with good surfaces, as shown in Figure 6.2.
Fig. 6.1. The vertical correction used for each track to minimize deviation of the head offset.

(a) Powder clad  
(b) Wire clad

Fig. 6.2. Images of the clad resulting from each feedstock.
6.3.1 In situ Measurement

Each plate is clamped into a fixture on one end to allow the free end to deflect and the substrate temperature to be measured (Figure 6.3). The in situ distortion measurements are taken with a Keyance LK-031 Laser Displacement Sensor (LDS), which has a range of 10 mm, a target diameter of 10 μm, and a resolution and accuracy of 1 μm. The temperature history during the build is measured at various locations on the sides and bottom of the substrate plate using Omega GG-K-30 type K thermocouples. These thermocouples have a measurement uncertainty equal to the larger value of 2.2°C or 0.75%. More detail on this measurement setup can be found in [99].

![Fig. 6.3. The experiment setup.](image)

The schematic diagrams in Figure 6.4 show the locations of the LDS target point and the thermocouples. Two thermocouples are attached to the bottom of the substrate,
with one (TC 1) located at the center point and the other (TC 2) located approximately 4 mm from the edge of the plate and half way along the length of the plate. Two additional thermocouples are placed on the side walls of the substrate as near the top surface as possible, with one (TC 3) located half way along the side face coinciding with the first pass while the other (TC 4) is located at the mid point of the side face coinciding with the last pass. One thermocouple (TC 5) is located on the face of the free end of the plate, along its midpoint as near the top surface as possible. The final thermocouple (TC 6) is located to the top surface, near the clamp.

6.3.2 Post Process Measurement

Each plate is sectioned post-process to investigate the clad quality and the melt pool dilution. Figure 6.5 shows that the section are removed from the center of the plate. The sections are mounted and polished and images are then acquired to measure the geometrical dilution of the melt pool which is defined as:

\[
\text{dilution} = \frac{b}{h + b}
\]  

(6.3)

where \( h \) is the height of the clad and \( b \) is the maximum depth of the melt pool into the substrate [145].
Fig. 6.4. Schematics of a) the top and b) the bottom surfaces of the plate showing the measurement locations.

Fig. 6.5. An illustration where sections are removed from each plate.
6.4 Finite Element Analysis

6.4.1 Thermal Model

A three dimensional transient thermal analysis [22] is used to investigate the temperature history of each deposition. FE analysis is beneficial for investigating process fundamentals because variables which are inter-connected during processing can be decoupled, allowing their effects to be studied. The thermal analysis is dependent upon the density ($\rho$), specific heat ($C_p$), and thermal conductivity ($k$) of the material. Except for the density, which is 8.44 g/cm$^3$, these properties are temperature dependent according to [146] and presented in Table 6.2. The governing heat transfer energy balance is:

$$\rho C_p \frac{dT}{dt} = -\nabla \cdot q(r,t) + Q(r,t)$$  \hspace{1cm} (6.4)

where the variables $T$, $t$, $Q$, and $r$ are the temperature, time, heat source, and relative reference coordinate. The heat flux vector, $q$, is calculated as:

$$q = -k \nabla T$$  \hspace{1cm} (6.5)

Heat loss occurs on all surfaces of the model through radiation, $q_{\text{rad}}$, and convection, $q_{\text{conv}}$. The Stefan-Boltzman law defines radiation:

$$q_{\text{rad}} = \varepsilon \beta (T_s^4 - T_\infty^4)$$  \hspace{1cm} (6.6)
Table 6.2. Temperature dependent thermal properties of Inconel® 625 [146].

<table>
<thead>
<tr>
<th>$T$ (°C)</th>
<th>$k$ (W/m/°C)</th>
<th>$C_p$ (J/kg/°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>21</td>
<td>9.8</td>
<td>410</td>
</tr>
<tr>
<td>93</td>
<td>10.8</td>
<td>427</td>
</tr>
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<td>204</td>
<td>12.5</td>
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<td>316</td>
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<td>427</td>
<td>15.7</td>
<td>511</td>
</tr>
<tr>
<td>538</td>
<td>17.5</td>
<td>536</td>
</tr>
<tr>
<td>649</td>
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<td>760</td>
<td>20.8</td>
<td>590</td>
</tr>
<tr>
<td>871</td>
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<td>620</td>
</tr>
<tr>
<td>982</td>
<td>25.2</td>
<td>645</td>
</tr>
</tbody>
</table>

where $\varepsilon$ is the emissivity of the material, $\beta$ is the Stefan-Boltzman constant, $T_s$ is the surface temperature, and $T_\infty$ is the ambient temperature. Emissivity is assumed to be independent of temperature and equal to 0.43 [147]. Heat loss through convection is:

$$q_{\text{conv}} = h(T_s - T_\infty)$$  \hspace{1cm} (6.7)

where the coefficient of convection is $h$.

6.4.2 Mechanical Model

A three dimensional quasi-static mechanical analysis is performed using the thermal simulation results as an input [22]. The analysis is dependent upon the elastic modulus ($E$), poisson’s ratio ($\nu$), coefficient of thermal expansion ($\alpha$), yield strength ($\sigma_y$), and ultimate strength ($\sigma_u$), which is measured at a strain of $\epsilon_u$. In the substrate, each of these properties is temperature dependent [146] and presented in Table 6.3.
Temperature independent yield strength (634 MPa) and ultimate strength (931 MPa), measured at a strain of 0.38, are used for the clad layer [148]. The stress equilibrium is governed by:

\[ \nabla \cdot \sigma = 0 \]  

(6.8)

where \( \sigma \) is stress. The mechanical constitutive law is:

\[ \sigma = C \epsilon_e \]  

(6.9)

\[ \epsilon = \epsilon_e + \epsilon_p + \epsilon_T \]  

(6.10)

where \( C \) is the fourth order material stiffness tensor and the variables \( \epsilon, \epsilon_e, \epsilon_p, \) and \( \epsilon_T \) are the total, elastic, plastic, and thermal strains.

Table 6.3. Temperature dependent mechanical properties of Inconel\textsuperscript{®} 625 [146].

<table>
<thead>
<tr>
<th>( T (^\circ \text{C}) )</th>
<th>( E (\text{GPa}) )</th>
<th>( \nu )</th>
<th>( \alpha (\mu\text{m/m/}^\circ \text{C}) )</th>
<th>( \sigma_y (\text{MPa}) )</th>
<th>( \sigma_u (\text{MPa}) )</th>
<th>( \epsilon_u )</th>
</tr>
</thead>
<tbody>
<tr>
<td>21</td>
<td>208</td>
<td>0.278</td>
<td>12.8</td>
<td>479</td>
<td>965</td>
<td>0.54</td>
</tr>
<tr>
<td>93</td>
<td>204</td>
<td>0.280</td>
<td>12.8</td>
<td>466</td>
<td>946</td>
<td>0.48</td>
</tr>
<tr>
<td>204</td>
<td>198</td>
<td>0.286</td>
<td>13.1</td>
<td>440</td>
<td>906</td>
<td>0.47</td>
</tr>
<tr>
<td>316</td>
<td>192</td>
<td>0.290</td>
<td>13.3</td>
<td>426</td>
<td>896</td>
<td>0.46</td>
</tr>
<tr>
<td>427</td>
<td>186</td>
<td>0.295</td>
<td>13.7</td>
<td>419</td>
<td>914</td>
<td>0.49</td>
</tr>
<tr>
<td>538</td>
<td>179</td>
<td>0.305</td>
<td>14.0</td>
<td>418</td>
<td>914</td>
<td>0.48</td>
</tr>
<tr>
<td>649</td>
<td>170</td>
<td>0.321</td>
<td>14.8</td>
<td>425</td>
<td>825</td>
<td>0.33</td>
</tr>
<tr>
<td>760</td>
<td>161</td>
<td>0.340</td>
<td>15.3</td>
<td>421</td>
<td>543</td>
<td>0.46</td>
</tr>
<tr>
<td>871</td>
<td>148</td>
<td>0.340</td>
<td>15.8</td>
<td>279</td>
<td>279</td>
<td>1.25</td>
</tr>
<tr>
<td>927</td>
<td>148</td>
<td>0.340</td>
<td>16.2</td>
<td>130</td>
<td>130</td>
<td>1.50</td>
</tr>
</tbody>
</table>
6.4.3 Numerical Implementation

6.4.3.1 Finite Element Mesh

The 3D mesh used for both the thermal and mechanical analysis is presented in Figure 6.6. The mesh consists of 107,038 nodes and 96,484 hex 8 elements. The aluminum clamp is included to capture the heat transferred from the substrate during deposition. The clad is modeled as a single layer of elements, with a height equal to the deposition thickness reported in Table 6.1. The width and length of each element in the clad is equal to half of the laser radius (1.016 mm). The convergence study performed by Gouge et al. on a similar geometry found this to be a sufficiently fine mesh [135]. Mechanical constraints are applied to the substrate at the clamp so it is free to deform like the cantilevered experiments. Comparisons are made between the model and experiments by extracting data from the nodes that correspond to the measurement points, as indicated in Figure 6.6.

![Fig. 6.6. The finite element mesh of the substrate, clad, and the fixture’s aluminum clamp.](image)

(a) Clad surface  
(b) Bottom surface
6.4.3.2 Heat Source

The double ellipsoid model described in [8] is used to prescribe the laser heat source:

\[
Q = \frac{6\sqrt{3} P \eta f}{abc \pi \sqrt{\pi}} e^{-\left(\frac{3x^2}{a^2} + \frac{3y^2}{b^2} + \frac{3(z+v+w)}{c^2}\right)}
\]

(6.11)

where \(x, y, \) and \(z\) are local coordinates, \(P\) is the laser power, and \(\eta\) is the laser absorption efficiency. The laser power is known but the absorption efficiency is not; however, it will be determined using inverse simulation [22]. The variables \(f, a, b,\) and \(c\) define the volume over which the heat source is distributed. The volume is defined such that the laser spot is circular with a radius equal to the laser radius and applied to a depth equal to the clad layer thickness.

6.4.3.3 Convection Model

The argon jets emitted from the deposition head generate forced convection on the substrate and clad surfaces which must be incorporated into the FE analysis. Gouge et al. [135] developed and validated a laser powder cladding model which incorporated measurement based convection [114] to account for the convection acting on the smooth substrate surface and rough powder clad surface, as shown in Figure 6.7. In this model, the convection acting on a smooth surface \((h_{\text{smooth}})\) is applied to the top surfaces of the substrate:

\[
h_{\text{smooth}} = 75.3e^{-0.047r} + 33.7
\]

(6.12)
while the convection acting on the rough surface \( (h_{\text{rough}}) \) is applied to the powder clad free surfaces:

\[
h_{\text{rough}} = 69.0e^{-0.070r} + 21.7 \quad (6.13)
\]

where \( r \) is the distance from the impingement point (laser center) in mm. A value of 9 W/m\(^2\)/°C is applied to all other surfaces. Because the wire clad surface is considerably smoother than the powder clad surface, as shown in Figure 6.2, the smooth convection model is applied to both the substrate top surface and the clad surfaces when modeling the wire clad.

![Graph showing convection measurements and applied models](image-url)

**Fig. 6.7.** The convection measurements and the applied models.
6.4.3.4 The FEA Solver

CUBIC, a FE solver developed specifically for modeling AM processes, is used to perform each analysis. The “quiet” element activation scheme is used such that all the elements in the deposition are included in the analysis but given material properties that minimize their impact on the analysis until they are activated [36]. Thermal conductivity ($k$), specific heat capacity ($C_p$), and elastic modulus ($E$) are scaled by factors of $10^{-6}$, $10^{-2}$, and $10^{-4}$ before becoming active, at which point the scaling factors are removed and the elements’ temperature is re-set to ambient to prevent erroneous heating [36]. The FEA solver also re-assess the free surfaces of the part at each time increment, ensuring that convection and radiation are applied to all exposed free surfaces and none of the internal surfaces.

6.5 Results and Discussion

6.5.1 Temperature History

The temperature history resulting from the clad using each feedstock material is presented in Figure 6.8. It is evident that the powder deposition experiences temperatures that are nearly 100 $^\circ$C higher than the wire deposition. In each of the other four thermocouple measurements, the powder clad results in higher temperatures.

Inverse simulation is used to determine the laser absorption efficiency of each case. Each plot in Figure 6.9 presents comparisons between Node 1, using three different values of laser absorption efficiency ($\eta$), and the measurements made by TC 1 (Figure 6.9). An absorption efficiency of 45% is found for the powder deposition, while 36%
Fig. 6.8. Temperature measurements using TC 1 and TC 2. Both are located on the bottom surface of the substrate.
is found for the wire deposition. Consequently, the wire clad absorbs 900 W while the powder clad absorbs 1125 W.

The higher efficiency of the powder deposition is due to the powder traveling through the laser beam, absorbing energy before it reaches the melt pool and allowing it to retain 25% more laser power compared to the wire clad. Other studies support this hypothesis. For example, Lin performed measurements and calculations to show that the powder particles are heated significantly by the laser beam [136]. In addition, Partes developed a model to show that the powder absorbs enough energy to melt, which in turn impacts the capture efficiency [137]. It has also be demonstrated that pre-placed scatters the laser beam energy within the powder, enabling more of the energy to be absorbed by the part [149].

6.5.2 Deflection History

Figure 6.10 presents the measured in situ deflection of each plate. In both depositions, the plate deflects downward in response to the thermal expansion of the material around the track, then deflects upward as the material cools and contracts. A net increase in deflection occurs after each track. The wire deposition concludes with 1.76 mm of deflection, while the powder deposition causes 2.15 mm of deflection, a difference of 0.39 mm, or 22%.

The mechanical simulation results are presented in Figure 6.11. Because the wire clad thickness is half of the powder clad thickness, two layers of each are simulated so that the deflection resulting from similar clad thickness can be compared. A 15 minute delay was imposed between layers, similar to the two layer clads performed in earlier
Fig. 6.9. The simulation results at Node 1 compared to TC 1 measurements for each case.

Fig. 6.10. Deflection measurements.
work [99]. It was not feasible to perform 2 layer depositions because of the difficulty with injecting the wire feedstock into a melt pool traversing across an already deformed substrate.

![Graph showing deformation over time for different materials and simulations.](image)

**Fig. 6.11.** The simulation deformation results of two layers compared to measurement.

Good agreement is achieved between the measurements and the simulations. In each case, the deflection after the first layer is slightly over-predicted when compared to the measurements. At approximately the 925 s mark, which is after the conclusion of the first layer, the simulation of the powder clad is 0.148 mm (6.9%) more than the measurement while the simulation of the wire clad is 0.126 mm (7.1%) greater than the measurement. Furthermore, the simulation of the second layer agrees well with the trends observed in the earlier work [99]. Using a the same laser power (2.5 kW), travel speed (10.6 mm/s), hatch spacing (2.03 mm), and powder feed rate (19.0 g/min), Heigel
et al. found that the second layer increased the total deflection by 78%. Similarly, the simulation of the second powder clad layer increases the total deflection by 86%.

The simulation results are used to assess the deflection for similar clad thicknesses using either powder or wire. A single layer of the powder clad results in a thickness of 1.42 mm and a deflection of 2.28 mm. Two layers of wire clad are required to achieve an approximately equal clad thickness of 1.48 mm. According to the simulation, this will result in a total deflection of 3.61 mm, which is significantly greater than the deflection caused by the single powder clad layer. The greater deflection resulting from wire cladding the desired thickness with two layers is caused by the greater amount of energy. The part absorbs 1.13 kW of energy during a single powder clad layer, while the part absorbs 1.8 kW of energy from the two wire clad layers. The 60% increase in energy results in an 80% increase in deflection. Therefore, using powder to clad a desired thickness will result in less part distortion.

6.5.3 Post-Process Clad Analysis

The images of the cross sections taken from each plate are shown in Figure 6.12. Each feedstock produce clads that are fully bonded with the substrate and free of voids. The melt pool dilution for each clad is calculated using Equation (6.3), where the powder melt pool dilution is 47% and the wire melt pool dilution is 43%. The greater dilution of the powder clad is a consequence of the higher laser absorption, which allows a greater amount of energy to reach the substrate.
(a) Powder clad  (b) Wire clad

Fig. 6.12. Cross sections of each clad. The black horizontal line indicates where the original substrate surface.

6.6 Conclusions

The difference in the thermal and mechanical response of substrates laser clad with either Inconel® 625 powder or wire feedstock is investigated. The temperature and deflection histories of single layer clad processes are measured in situ and the residual stress of the substrate is measured post-process. A thermo-mechanical FE model is developed and validated to investigate the laser absorption efficiency of each feedstock and to simulate the deflection resulting from multi-layer cladding.

The results show that despite both processes utilizing the same laser power, travel speed, and hatch spacing, significant differences occur between laser cladding with powder or wire. For instance, cladding with powder generates substrate temperatures approximately 100 °C greater than those resulting from cladding with wire. The FE model is used to demonstrate that the powder feedstock enables a greater amount of laser energy to be absorbed (45% compared to 36%). In addition, laser powder cladding of a single 1.42 mm thick layer causes the substrate to deflect more than laser wire
cladding a single 0.78 mm thick layer. However, FE simulation results of a second layer clad with wire reveal that in fact when each feedstock is used to clad a similar thickness, the wire generates 85% more deflection. Images of the cross sections are used to confirm the quality of each clad and to investigate the melt pool dilution. Measurements of the dilution reveal that the powder cladding results in more dilution compared to wire cladding. This is attributed to greater amount of absorbed laser power, which has been shown in the literature to be directly related to the melt pool depth.

Considering the results from this study, it is recommended that powder cladding be chosen over wire cladding when the surface finish is not critical because it produces less distortion for a given clad thickness.

6.7 Acknowledgments

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