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An improved prediction of residual stresses and distortion in additive manufacturing

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ABSTRACT

In laser assisted additive manufacturing (AM) an accurate estimation of residual stresses and distortion is necessary to achieve dimensional accuracy and prevent premature fatigue failure, delamination and buckling of components. Since many process variables affect AM, experimental measurements of residual stresses and distortion are time consuming and expensive. Numerical thermo-mechanical models can be used for their estimation, but the quality of calculations depends critically on the accurate transient temperature field which affects both the residual stresses and distortion. In this study, a well-tested, three-dimensional, transient heat transfer and fluid flow model is used to accurately calculate transient temperature field for the residual stress and distortion modeling. The calculated residual stress distributions are compared with independent experimental results. It is shown that the residual stresses can be significantly minimized by reducing the layer thickness during AM. Inconel 718 components are found to be more susceptible to delamination than Ti-6Al-4V parts because they encounter higher residual stresses compared to their yield strength.

1. Introduction

The additive manufacturing (AM) process involves heating, melting and solidification of an alloy by a moving heat source such as a laser or an electron beam in a layer by layer manner [1,2]. As a result, different regions of the work piece experience repeated heating and cooling [2]. The spatially varied thermal cycles result in residual stresses and distortion in the additively manufactured components [3]. The residual stresses, whose magnitude can exceed the yield strength of the alloy, affect corrosion resistance, fracture toughness, crack growth behavior and fatigue performance [4–8]. Moreover, the residual stresses are associated with pronounced deformations especially for thin-walled features [9–13]. Because AM involves many process variables as is the case with fusion welding, experimental measurements of stresses and strains are expensive and time consuming [4,14]. Moreover, experimental measurements depend on the shape and size of the components, nature of the stresses measured, sample preparation and accuracy of X-ray or neutron diffraction [7,14]. A recourse is to undertake calculations of residual stresses and strains in all locations of the work piece [15]. These calculations are often done in two steps in sequence. First, the transient temperature field in the entire work piece is calculated. The computed temperature results are then used for the mechanical calculations. Such sequential calculations of temperatures and stresses make the computations tractable but the accuracy of the calculations depends critically on the quality of the transient temperature field and the thermo-physical property data of the alloy.

As the laser or electron beam energy impinges on the work piece surface, the powder melts quickly to form a molten pool. The highest temperature on the molten pool surface is attained directly below the heat source and the temperature decreases with distance away from this location [16,17]. Inside the molten pool, the liquid alloy recirculates rapidly at very high velocities driven by the spatial gradient of surface tension. The convective flow mixes the liquid metal in different regions and enhances the transport of heat within the molten pool. The circulation pattern strongly affects the temperature distribution in the liquid alloy, heating and cooling rates, solidification pattern, and subsequently the evolution of various solid phases that make up the final microstructure of the part [18,19].

Simulation of complex physical processes that affect the temperature field is computationally intensive, and many of the previous calculations of the temperature field involved various simplifications and assumptions to make the calculations tractable. These include several two-dimensional models [20,21], or an

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Table 1
Thermo-physical properties of Ti-6Al-4V and IN 718 [30].

Properties	Ti-6Al-4V	IN 718
Liquidus temperature (K)	1928	1609
Solidus temperature (K)	1878	1533
Thermal conductivity (W/m K)	$1.57 + 1.6 \times 10^{-2}$ T $- 1 \times 10^{-6}$ T^2	$0.56 + 2.9 \times 10^{-2}$ T $- 7 \times 10^{-6}$ T^2
Specific heat (J/kg K)	$492.4 + 0.025$ T $- 4.18 \times 10^{-6}$ T^2	$360.24 + 0.026$ T $- 4 \times 10^{-6}$ T^2
Density (kg/m^3)	4000	8100
Viscosity (kg/m s)	4×10^{-3}	5×10^{-3}
dγ/dT (N/m K)	-0.37×10^{-3}	-0.26×10^{-3}

assumption that the entire deposit is heated and then cooled [22], or building a part by a single layer deposition [23]. In some instances, heat sources have been simplified as surface flux to achieve computational speed [3,23–25]. Another common difficulty is that the calculations ignore the convective heat transfer which is the main mechanism of heat transfer within the liquid, as discussed previously [16–19]. This simplification can lead to the use of inaccurate temperature field for thermo-mechanical calculations [26–28], and the computed residual stress and strain fields do not always agree well with the corresponding experimental data.

The errors in the transient temperature fields and heating and cooling rates resulting from heat conduction calculations that ignore the molten metal convection are well documented in the literature. Svensson et al. noted that "...the heat conduction equation has been found to be inadequate in representing experimental cooling curves" [29]. Manvatkar et al. [16] showed that by ignoring the effect of convection, the cooling rates in additive manufacturing were over-estimated by about twice of the correct values. Therefore, the temperature distribution calculated using heat conduction models without extensive experimental calibrations is not accurate, which in turn, can adversely affect the accuracy in calculations of residual stresses and distortion. What is needed and not currently available is a numerical model that calculates residual stress and strain fields from the transient temperature distribution considering convective heat transfer.

Here we combine a well-tested three-dimensional transient heat transfer and fluid flow model of additive manufacturing with a thermo-mechanical model to accurately calculate the temperature fields, residual stresses and distortion. The calculated temperature and residual stress distributions are tested using independent experimental results. After validation, the model is used to quantitatively study the effect of a wide variety of AM variables such as heat input and layer thickness on residual stresses and distortion. Although the results shown in this article are for direct energy deposition process, the findings will be useful to make dimensionally compliant components and assess residual stresses for all laser assisted powder based AM processes.

2. Modeling

2.1. Assumptions

Some simplified assumptions are made in both the heat transfer and fluid flow model and the thermo-mechanical model. The densities of the solid and liquid metals are assumed to be constant. The surfaces of the deposited layers are considered to be flat. The loss of alloying elements due to vaporization and its effects on both the heat loss and composition change are not incorporated in the present calculations. Finally, the effects of strains induced by solid-state phase transformation and creep are also neglected.

2.2. Governing equations

A well tested, three dimensional, transient, heat transfer and fluid flow model for AM [16,17] is used to compute temperature and liquid metal velocity fields. The model solves the following equations of conservation of mass, momentum and energy [18,19] in three dimensions.

$$\frac{\partial(\rho u_i)}{\partial x_i} = 0 \quad (1)$$

$$\frac{\partial(\rho u_j)}{\partial t} + \frac{\partial(\rho u_j u_i)}{\partial x_i} = \frac{\partial}{\partial x_i}\left(\mu \frac{\partial u_j}{\partial x_i}\right) + S_j \quad (2)$$

where ρ is the density, u_i and u_j are the velocity components along the i and j directions, respectively, and x_i is the distance along the i direction, t is the time, μ is the effective viscosity, and S_j is a source term for the momentum equation. The energy conservation equation is:

$$\rho\frac{\partial h}{\partial t} + \frac{\partial(\rho u_i h)}{\partial x_i} = \frac{\partial}{\partial x_i}\left(\frac{k}{C_p}\frac{\partial h}{\partial x_i}\right) - \rho\frac{\partial \Delta H}{\partial t} - \rho\frac{\partial(u_i \Delta H)}{\partial x_i} \quad (3)$$

where h is the sensible heat, C_p is the specific heat, k is the thermal conductivity, and ΔH is the latent heat content. Table 1 shows the thermo-physical properties of the alloys used for the calculations.

The temperature field as a function of time calculated from the heat transfer and fluid flow model is then imported to a mechanical model based on Abaqus®, a commercial finite element analysis (FEA) code [31]. The total strain increment ($\Delta\varepsilon_{lm}^{tot}$) with respect to time is contributed by the several effects shown below:

$$\Delta\varepsilon_{lm}^{tot} = \Delta\varepsilon_{lm}^{E} + \Delta\varepsilon_{lm}^{P} + \Delta\varepsilon_{lm}^{Th} + \Delta\varepsilon_{lm}^{V} \quad (4)$$

where $\Delta\varepsilon_{lm}^{E}$, $\Delta\varepsilon_{lm}^{P}$ and $\Delta\varepsilon_{lm}^{Th}$ are the elastic, plastic and thermal strain increments respectively. $\Delta\varepsilon_{lm}^{V}$ is the strain induced due to the solid state phase transformation and creep, which is assumed to be zero in the present model. The resulting stress increment estimated from the elastic strain as [5]:

$$\Delta\sigma_{ij}^{E} = D_{ijlm} \cdot \Delta\varepsilon_{lm}^{E} \quad (5)$$

where D_{ijlm} is the elastic stiffness matrix calculated from Young's modulus (E) and Poisson's ratio (υ) as,

$$D_{ijlm} = \frac{E}{1+v}\left[\frac{1}{2}(\delta_{ij}\delta_{lm} + \delta_{il}\delta_{jm}) + \frac{v}{1-2v}\delta_{ij}\delta_{lm}\right] \quad (6)$$

where δ is a Dirac delta function [32] whose value is one only for $i = j$ and $l = m$, and is zero otherwise. Temperature-dependent plasticity with the von Mises yield criterion [5] is utilized to model the flow stress and plastic strain. The thermal strain increment is calculated as:

$$\Delta\varepsilon_{lm}^{Th} = \beta\delta_{lm}\Delta T \quad (7)$$

where β is the volumetric thermal expansion coefficient and ΔT is the temperature increment. The temperature-dependent mechanical properties used for the calculations for Inconel 718 and Ti-6Al-4V are given in Tables 2 and 3, respectively. The step-by-step procedure for calculating temperature distribution, residual stresses and distortion is illustrated in Table 4. A Python script was developed to facilitate mapping the transient temperature fields from the heat transfer and fluid flow model to the Abaqus-based