Process modelling for selective laser melting additive manufacturing

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SINGAPORE

**PROCESS MODELLING FOR SELECTIVE LASER
MELTING ADDITIVE MANUFACTURING**

TAN PENGFEI

SCHOOL OF MECHANICAL AND AEROSPACE ENGINEERING

2020

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TAN PENGFEI

SCHOOL OF MECHANICAL AND AEROSPACE ENGINEERING

A thesis submitted to the Nanyang Technological University in
partial fulfilment of the requirement for the degree of
Doctor of Philosophy

2020

Statement of Originality

I hereby certify that the work embodied in this thesis is the result of original research, is free of plagiarised materials, and has not been submitted for a higher degree to any other University or Institution.

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I have reviewed the content and presentation style of this thesis and declare it is free of plagiarism and of sufficient grammatical clarity to be examined. To the best of my knowledge, the research and writing are those of the candidate except as acknowledged in the Author Attribution Statement. I confirm that the investigations were conducted in accord with the ethics policies and integrity standards of Nanyang Technological University and that the research data are presented honestly and without prejudice.

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Assoc Prof Zhou Kun

Authorship Attribution Statement

This thesis contains material from two papers published in the following peer-reviewed journal(s) / from papers accepted at conferences in which I am listed as an author.

Chapter 3 is published as P. Tan, F. Shen & K. Zhou, A thermo-mechanical coupling model for predicting temperature and residual stress fields in selective laser melting, *The 3rd International Conference on Progress in Additive Manufacturing, Singapore* (2018).

The contributions of the co-authors are as follows:

- Associate Professor Zhou proposed the idea, analyzed the results and revised the manuscript.
- I developed the simulation model, conducted the simulation, obtained and analyzed the results and drafted the manuscript.
- Dr. Shen analyzed the results and revised the manuscript.

Chapter 4 is published as P. Tan, F. Shen, B. Li & K. Zhou, A thermo-metallurgical-mechanical model for selective laser melting of Ti6Al4V, *Materials & Design* **168** (2019) 107642.

The contributions of the co-authors are as follows:

- Associate Professor Zhou initiated the idea, analyzed the results and revised the manuscript.
- I developed the simulation model, conducted the simulation and analysis and drafted the manuscript.
- Dr. Shen analyzed the results and revised the manuscript.

- Dr. Li analyzed the results and revised the manuscript.

16/03/2020

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Date

Tan Pengfei

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Tan Pengfei

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Abstract

Selective laser melting (SLM), a typical additive manufacturing technique, selectively fuses metallic powders to build products via a layer-wise method from computer-aided-design models. The SLM technique has shown great potential for aerospace, automobile and biomedicine industries. However, SLM-built products suffer from drawbacks such as poor surface finish, thermal distortion, cracks and unsatisfactory mechanical strength. In the SLM additive manufacturing process, there exist complex multi-scale and multi-physics phenomena including laser interaction with powders, heat and mass transfer, thermal distortion, multi-phase flow and microstructural evolution. The representative parameters of these phenomena are difficult to measure through experiments. Alternatively, numerical simulation provides a feasible and efficient way to predict these process parameters and reveal the underlying mechanism of defect formation within metals. Therefore, this Ph.D. research aims to develop numerical models to study the thermal, mechanical and fluid-dynamic behaviours of stainless steel 316L and Ti6Al4V in the SLM additive manufacturing process.

Firstly, a three-dimensional thermo-mechanical coupling model based on the finite element method has been developed to simulate the multi-layer and multi-track SLM process of stainless steel 316L. The model considers temperature-dependent material properties and the powder-liquid-solid phase transition which includes melting, vaporization, solidification, shrinkage and cooling phenomena. Temperature and stress fields in the SLM-built specimen can be predicted by solving the heat conduction and stress equilibrium equations. The thermal analysis reveals that both increasing laser power and

reducing scanning speed can lead to the increase of the melt pool size. Furthermore, the mechanical analysis suggests that the stress component along the scanning direction is larger than the stress components in the other two directions. Large residual stresses are distributed in the first two layers of a multi-layer printed product due to the constraints from the substrate.

Secondly, a thermo-metallurgical-mechanical coupling model has been developed to predict temperature, solid-state phase and residual stress fields in Ti6Al4V for the SLM additive manufacturing process. The thermal analysis is based on the transient heat conduction problem with a volumetric heat source describing the laser absorption and scattering in the powder bed. The volume fraction of metallurgical phases is determined by the temperature field and used to obtain the volumetric change strain due to the solid-state phase transformation (SSPT). An elasto-plastic constitutive law considering the strains induced by thermal gradients and SSPT has been proposed to evaluate stress fields. Modelling results conclude that the consideration of SSPT leads to the decrease of tensile residual stresses and the increase of compressive residual stresses.

Finally, a powder-scale multi-physics model has been developed to investigate the melt pool dynamics and porosity for stainless steel 316L additively manufactured by SLM. The effects of the laser power, laser scanning speed and ambient pressure on the thermo-fluid behaviour and pore formation in the melt pool are analyzed. Simulation results reveal that excessive laser energy may cause the formation of keyhole-induced pores, while inadequate laser intensity may give rise to irregular pores at the interlayer. Additionally, the reduced ambient pressure can increase the melt pool depth, whereas the increase is not noticeable below a critical ambient pressure. As compared with the atmospheric pressure,

the sub-atmospheric pressure contributes to a lower average temperature and larger average pressure on the keyhole surface. Consequentially, the sub-atmospheric pressure can increase the keyhole stability and thus minimize the pores in SLM-built products.

In this Ph.D. research, thermal, mechanical and fluid dynamic models have been developed to predict the temperature, stress and fluid flow fields during the SLM additive manufacturing process. The numerical models can provide insight into the physical understanding of the metal additive manufacturing process. The simulation results can be further utilized to optimize the processing parameters for tailoring the mechanical properties of SLM-built products.

List of Publications

- [1] **P. Tan**, F. Shen, B. Li & K. Zhou, A thermo-metallurgical-mechanical model for selective laser melting of Ti6Al4V, *Materials & Design* 168 (2019) 107642.
- [2] Y. Li, K. Zhou, **P. Tan**, S. B. Tor, C. K. Chua & K. F. Leong, Modeling temperature and residual stress fields in selective laser melting, *International Journal of Mechanical Sciences* 136 (2018) 24-35.
- [3] **P. Tan**, F. Shen & K. Zhou, A thermo-mechanical coupling model for predicting temperature and residual stress fields in selective laser melting, *The 3rd International Conference on Progress in Additive Manufacturing*, Singapore (2018).
- [4] **P. Tan**, Y. L. Li, & K. Zhou, Finite element analysis of temperature field in selective laser melting process, *The 2nd International Conference on Progress in Additive Manufacturing*, Singapore (2016).

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List of Symbols

Symbol	Physical meaning
T	Temperature
ρ	Density
k	Heat conductivity
H	Enthalpy
P	Laser power
v	Laser scanning speed
r_0	Laser beam radius
h	Powder layer thickness
σ	Stress tensor
ε	Strain tensor
α_T	Coefficient of thermal expansion
φ	Porosity
C	Specific heat capacity
$\vec{u}$	Velocity vector
$\dot{m}$	Rate of mass transfer
$\vec{g}$	Gravity acceleration
μ	Dynamic viscosity
R	Universal gas constant
σ_s	Stefan-Boltzmann constant
ε	Emissivity
A	Laser absorptivity

Chapter 1. Introduction

Selective laser melting (SLM), one of the most promising additive manufacturing techniques, has recently attracted extensive attentions due to the increasing demands of customized and low-cost products. However, SLM-built products suffer from disadvantages such as poor surface finish, thermal distortion and unsatisfactory mechanical strength. To overcome these drawbacks and improve mechanical performance of products, it is vital to determine the physical mechanism of process-induced defects. In this chapter, a brief introduction to SLM is conducted. Subsequently, the motivations and objectives of this Ph.D. research are presented and the organization is outlined.

1.1. Background

SLM is capable of fabricating customized and near-net-shape products without the waste of materials [1]. Theoretically, geometrical structures built by SLM are not restricted owing to the characteristics of the additive manufacturing process. Furthermore, products can be additively manufactured by SLM without the need of the conventional processing steps such as casting moulds and tooling. Hence, the design and production time is significantly reduced [2]. Because of these advantages, SLM has become a promising manufacturing technique for high-performance products [3]. Additionally, SLM is an ideal candidate for fabricating functional prototypes and specific structures with complex geometries such as hollow geometry and cellular lattice structures.

While significant advances have been achieved for the processing technique of SLM, SLM-built products suffer from defects such as thermal deformation, cracks, porosity and

rough surface, as shown in Fig. 1.1. These drawbacks are ascribed to complex multi-physics and multi-scale phenomena in the SLM process such as the laser interaction with the powder bed [4, 5], interaction between metal vapor and powders [6, 7], multi-phase flow [8-10], thermal distortion [11-13] and microstructural evolution [14-16]. To fabricate defect-free products, it is required to have a deep physical understanding of defects. Therefore, in terms of the difficulty to measure processing parameters and high cost for the experiment, process modelling provides a reasonable and efficient route to give insight into the formation mechanisms of product defects.

Fig. 1.1. Defects in additively manufactured products: (a) pores in the cross section of stainless steel 316L [17], (b) long and short cracks in the Rene88DT alloy [18], (c) delamination [19] of M2 high speed steel and (d) surface roughness of Inconel 718 [20].

Thermal behaviour is the foundation of mechanical, fluid-dynamic and microstructural analyses in the process modelling of SLM. The transient thermal diffusion process of SLM contains three heat transfer modes: heat conduction, heat convection and

radiation. Heat conduction is the primary mode of heat diffusion that exists in the powder bed and substrate. After metals are fused, heat convection plays a key role in the melt pool. Furthermore, the laser interaction with powders provides the heat source of metal fusion.

As a distinguishing feature in the SLM process, the high-temperature gradient may lead to residual stresses that are detrimental to the mechanical strength and structural integrity. Additionally, residual stresses may result in the product deformation, micro cracks and interlayer/interface debonding. Therefore, stress analysis is a vital issue to the reliability of high-quality products. In the SLM process, the heated metals expand and meanwhile are restricted by the neighboring materials with a low temperature. As the yield strength is reached, the low-temperature metals are plastically compressed. During the cooling process, the heated metals start to contract, but the plastic metals cannot recover to the initial condition. Hence, residual stresses are formed in the unrecovered metals. Furthermore, the residual stresses may be induced by uncontrolled solid-state phase transformation (SSPT) [21, 22]. The stress evolution in the manufacturing process is dependent on the following factors [23]: product geometry, materials, laser scanning pattern, environmental factors and processing parameters.

The fluid-dynamic behaviours of the SLM process play a crucial role in the formation of defects such as porosity [24-26], poor surface roughness [10], spattering [25] and balling [8] phenomena. The thermo-fluid modelling requires a multi-physics computational fluid dynamics (CFD) model that is developed based on three governing equations: mass, momentum and energy conservation equations. Furthermore, the level-set method [27] or volume of fluid (VOF) method [28, 29] is employed to track the liquid/gas interface. The driven forces inside the melt pool consist of surface tension, Marangoni's flow, recoil

pressure, damping force in the liquid and solid mushy zone, gravitational force and buoyancy force.

The most commonly used method for the process modelling of SLM is the finite element method (FEM), which can carry out thermal and mechanical analyses of printed products. In the FEM model, powders are assumed as a homogeneous continuum solid with effective thermomechanical properties. Furthermore, most of FEM models only consider the heat conduction but neglect the heat convection. In general, the heat convection in the melt pool of SLM is simulated by developing the CFD model based on the finite difference method or finite volume method. In the CFD model, discrete powders can be considered. To represent the discrete nature of powders, several meso-scale models are developed [8, 25, 30]. Recently, the CFD model coupled with the discrete element method (DEM) has been applied to the SLM process to simulate the recoating and laser melting processes of the powder bed [26, 31-34]. A two-dimensional (2D) and three-dimensional (3D) multi-physics software based on arbitrary Lagrangian-Eulerian techniques has been presented [35-37]. The software package addresses multi-physics engineering problems by combining the finite element and finite volume methods. Körner *et al.* [38, 39] proposed a 2D lattice Boltzmann model with the local stochastic powder configuration under the effects of capillary force and wetting behaviours.

1.2. Motivations

To conduct the stress analysis for SLM-built products, the temperature evolution during the printing process needs to be obtained. Afterwards, a thermo-mechanical coupling model is required to evaluate stress fields. Development of a reliable thermo-

mechanical coupling model is of prime significance in residual stress analysis because there exists a complex interaction between thermal and mechanical behaviours in the printed products during the SLM process. So far, most research has focused on the single-layer printing process [40, 41] and there is a lack of modelling of a multi-layer SLM process [42]. The thermal and mechanical interaction between two consecutive layers can be considered in the multi-layer SLM process, which is of considerable significance for the evolution of thermal stresses. Although the thermal-related behaviours such as volume shrinkage of the powder bed [43], powder-liquid-solid transition [44] and vaporization [45] have been considered individually, the consideration of these phenomena in a systematic thermo-mechanical model is scarcely reported.

Despite great progress in the current thermo-mechanical model, the metallurgical SSPT has not yet been considered for the stress analysis in the SLM process. The metallurgical SSPT process, which is often associated with the change of material properties, volumetric change and transformation-induced plasticity, has been recognized as a crucial factor for the formation of residual stresses, especially for some carbon steels and titanium alloys [46-48]. Pioneering works about the stress analysis in consideration of SSPT has been conducted in the laser welding process [49-52] and direct laser deposition process [48, 53]. However, as a powder-bed manufacturing technique, SLM involves more complex physicochemical phenomena such as the laser-powder interaction, powder-liquid-solid transition and volume shrinkage of the powder bed. For a multi-track and multi-layer SLM process, the printed product repeatedly undergoes SSPT due to preheating and reheating from adjacent tracks or layers, which increases the complexity and significance of the phase transformation [54]. Additionally, SLM exhibits a high-temperature gradient

and rapid solidification and cooling rates, which are of great importance to microstructural phase formation and characteristics [55].

In the SLM process, there exist complex multi-physics phenomena which are influenced by considerable processing parameters and environmental factors [36]. Ambient pressure is a crucial factor for manufacturing defect-free products by affecting the melting, solidification and vaporization process of metals. The laser melting technology of metals in the sub-atmospheric pressures have several advantages such as the improved penetration depth, more stable melt pool, decreased porosity and sound melt track [56-59]. Few studies have been conducted to explore the SLM process of metals under sub-atmospheric pressures. These works have focused on the influences of ambient pressures on the interaction of vapor plume and powders [7, 56, 60], microstructure [61, 62] and surface morphology [62] in the SLM process.

The process modelling of SLM can give a good understanding of the formation mechanism of process-induced defects, which is essential for the optimization of processing parameters. Furthermore, a comprehensive understanding of the physical mechanism is important to acquire high-quality products.

1.3. Objectives

This Ph.D. research aims to investigate the thermal, mechanical and fluid-dynamic behaviours of stainless steel 316L and Ti6Al4V during the SLM additive manufacturing process and analyze the formation mechanism of residual stresses and porosity within products. The main objectives of this research are outlined as follows:

- Establish a thermo-mechanical model for predicting the temperature and residual stress fields in the SLM-built products;
- Develop a thermo-metallurgical-mechanical model to explore the effect of SSPT on residual stresses;
- Investigate the thermo-fluid dynamic behaviours of the melt pool during the SLM process, as well as study the pore formation mechanisms of SLM-built products.

1.4. Dissertation outline

The Ph.D. dissertation consists of six chapters. In Chapter 2, a literature review on the process modelling for SLM is conducted. Chapter 3 presents a thermo-mechanical model to predict the temperature and residual stress fields for a multi-track and multi-layer SLM process. Chapter 4 develops a thermo-metallurgical-mechanical model to study the influence of SSPT on residual stresses. Chapter 5 applies CFD to explore the thermo-fluid behaviour of the SLM process with the consideration of mesoscale powders. Finally, Chapter 6 draws the main conclusions of this research work and recommends future research.

Chapter 2. Literature Review

In the SLM additive manufacturing process, there exist complex multi-scale and multi-physics phenomena such as laser interaction with powders, heat and mass transfer, thermal distortion, multi-phase flow and microstructural evolution. However, the representative parameters of these phenomena are difficult to measure through the experiment. Numerical simulation can provide a reasonable way to estimate these process parameters and understand the physical mechanism of product defects. In this chapter, the fundamentals of SLM is first introduced. Afterwards, two numerical approaches including FEM and CFD are adopted for conducting the process modelling of SLM. A review on the thermal, mechanical and melt flow behaviours of printed products in the SLM process is also conducted.

2.1. Selective laser melting additive manufacturing

SLM is used to produce metallic products layer upon layer from a 3D computer-aided-design (CAD) model via a high-intensity laser beam. Products manufactured by the SLM are increasingly applied to the automotive, aerospace and medicine industrial fields due to their outstanding mechanical performances including the excellent strength-to-weight ratio, wear resistance, high hardness and densification level [63].

The SLM process consists of multiple steps from building a CAD model to the post processing of printed products. A schematic of the SLM machine is demonstrated in Fig. 2.1. At the beginning, a CAD model needs to be established and then transformed to the slice data which defines the geometry of each printed layer. In the printing process, a metal substrate is utilized to support the printing product during the manufacturing process.

Powders are spread on the substrate or previously printed layers and subsequently melted by the laser beam. The laser beam selectively melts the powder bed based on the CAD model with the information of each sliced layer. Once one layer is completed, the substrate is lowered one-layer thickness and then the next layer of powders is deposited. The process is repeated to form the desired product [64]. The printing chamber is usually filled with nitrogen or argon to protect the product from oxygen, spattering and metal vapor plume [36]. The post processes such as polishing and heat treatment may be needed to strengthen the mechanical properties of printed products. Although a wide range of metals, including iron, titanium, nickel, aluminium, copper and magnesium, have been tested for SLM, only limited materials are well processed. The most commonly used metals with well-established processing parameters are stainless steel 316L and Ti6Al4V [65], which are used to conduct the process modelling in this work.

Fig. 2.1. A schematic of the SLM machine.

The complex physics of the SLM process covers broad temporal and spatial scales. It is reported that the SLM process involves about 130 parameters that may influence the quality of the printed products [66]. The main processing parameters consists of laser power, scanning speed, laser beam size, hatch distance, layer thickness, powders and scanning strategies. As compared with the conventional laser welding process, SLM has similarities but undergoes the effect of the powder bed. The laser interaction with powders, including radiation absorption and scattering, is an important phenomenon which directly influences the laser heat flux distribution. Nevertheless, it is quite difficult to directly measure the laser absorptivity of powders. Hence, the theoretical analysis and numerical simulation of the laser interaction with powders are widely adopted [67-69].

Numerical simulation is a feasible way to understand the formation mechanisms of the complex phenomena in the SLM process [70]. The numerical modelling can cover a wide range of spatial scales including the microscale, mesoscale and macroscale. In terms of the micro-scale modelling, the microstructure of the printed products such as grain growth can be predicted using cellular automata [14, 71] and phase field [72, 73]. For the mesoscale, the laser interaction with powders and fluid flow interaction with the powders dynamics can be simulated. In the macro scale, the mechanical behaviours of printed products including the thermal deformation and fracture mechanics can be investigated.

2.2. Numerical approaches for selective laser melting modelling

The process modelling of this research focused on the thermal, stress and fluid flow analyses. FEM is used to explore the thermo-mechanical behaviours of printed products. Additionally, CFD is applied for studying the thermo-fluid behaviours in the melt pool.

2.2.1. Finite element analysis

FEM is extensively employed to solve the differential and integral equations in the engineering problems. The basic idea of FEM is to regard the given domain as an assemblage of simple finite elements. The approximation functions are applied to represent the differential equation by the variational or weighted residual methods. As for SLM modelling, FEM is typically used to solve the heat transfer and stress equilibrium equations. In the weak-form Galerkin model, the heat conduction equation discretized by FEM is expressed by [74]

$$\mathbf{M}^e(\mathbf{T}^e)\dot{\mathbf{T}}^e + \mathbf{K}^e(\mathbf{T}^e) = \mathbf{Q}^e(\mathbf{T}^e) - \mathbf{q}^e(\mathbf{T}^e), \quad (2.1)$$

where $\dot{\mathbf{T}} = \partial \mathbf{T} / \partial t$ denotes a derivative with time, and

$$M_{ij}^e = \int_{\Omega^e} \rho C \varphi_i \varphi_j d\mathbf{x}, \quad K_{ij}^e = \int_{\Omega^e} k_{mn} \frac{\partial \varphi_i}{\partial x_m} \frac{\partial \varphi_j}{\partial x_n} d\mathbf{x}, \quad (2.2)$$

$$Q_i^e = \int_{\Omega^e} \varphi_i Q(\mathbf{x}, t) d\mathbf{x}, \quad q_i^e = \oint_{\Gamma^e} \varphi_i (q_a + q_c + q_r) ds, \quad (2.3)$$

where ρ is the density; C is the specific heat; φ is the basis function (polynomial in FEM); k_{mn} is the heat conductivity with $m, n = 1, 2, 3$; Q is the internal heat source; q_a , q_c , and q_r are the applied flux, convection flux and radiation flux on the boundary surface, respectively. Besides, the FEM model for mechanical analysis is written as

$$[K^e] \{u^e\} = \{F^e\}, \quad (2.4)$$

where $[K^e]$ denotes the stiffness matrix, $\{u^e\}$ represents the displacement vector, and $\{F^e\}$ is the force vector.

FEM can be used to conduct the macro-scale thermal and mechanical analyses of SLM-built products [75-78]. In the FEM model, the powder bed is usually assumed to be homogenous solids with effective material properties by neglecting the discrete nature of

powders. Hence, FEM is capable of carrying out part-scale modelling such as thermal deformation and fatigue analysis. FEM has been widely applied for modelling the SLM printing process, including the evolution of the temperature field [79-83] and thermal stresses [41, 50, 52, 84, 85]. FEM as a powerful numerical approach to model mechanical behaviours has been commonly used for the damage mechanics analysis of other laser melting techniques such as laser welding [86-89] and direct laser deposition [90-92]. However, such studies on SLM-built products are relatively scarce [93-95].

2.2.2. Computational fluid dynamics

CFD is a numerical mathematical method to analyze and solve fluid flow problems. The CFD model to describe the fluid flow behaviours is based on Navier–Stokes equations. In the CFD model, the differential equations that govern the transport of mass, momentum and energy need to be discretized and then solved. By solving the fundamental governing equations, CFD can acquire the representative parameters of the melt pool including the temperature, velocity, pressure and phase mixing rates. The common numerical approaches used for CFD problems include finite volume method, finite difference method and FEM. CFD is applied to research and engineering problems in extensive industrial fields, including aerospace, weather forecast, automotive, biology and ships.

The general differential equation for the CFD model is defined as [96]

$$\frac{\partial(\rho\phi)}{\partial t} + \text{div}(\rho\vec{u}\phi) = \text{div}(\Gamma_\phi \text{grad}\phi) + S_\phi, \quad (2.5)$$

where ρ denotes the density, ϕ represents the general variables including velocity in the three directions u , v , w , and temperature T , $\vec{u} = (u, v, w)$ is the velocity vector, Γ_ϕ is the diffusion coefficient and S_ϕ is the source term. In this general equation, four terms from left

to right are called the unsteady, convection, diffusion and source terms, respectively. The mass, momentum, energy conservation equations can be obtained by replacing the general variable ϕ with the dependent variables such as u , v , w and T . In particular, the mass conservation equation is obtained by letting $\phi = 1$ and $S_\phi = 0$.

In the SLM process, fluid problems include the melt pool flow and metal vapor plume. The metal vapor flow has been widely studied for laser welding, but it is scarcely reported for SLM [6, 60]. In terms of the melt flow, numerous studies have been recently conducted [24, 30, 97, 98]. The melt flow is associated with the pore formation, balling, denudation, spattering and surface roughness phenomena. In the CFD model, the powder bed can be assumed to be composed of homogenous materials or discrete powders. By coupling with the powder packing method such as DEM, the CFD model can be used for the powder-scale simulation.

2.3. Process modelling for selective laser melting

The process modelling can uncover the complex formation mechanism of the process-induced defects such as residual stresses, porosity, spattering and balling. In this section, thermal, mechanical and melt flow behaviours in the SLM process are analyzed.

2.3.1. Thermal behaviours

Thermal behaviours are vitally important to the SLM process because it is associated with many complex physical phenomena including the laser absorption of powders and the melting, vaporization and solidification of metals. The high-temperature gradient may lead to residual stresses and thermal deformation which influence the dimensional accuracy and mechanical performance of printed products. The heat transfer processes of SLM include

the laser radiation absorption of the powder bed, heat conduction in the powder bed and substrate and heat convection in the melt pool.

2.3.1.1. Laser heat source

For the SLM modelling, the laser radiation absorption is described by the heat source model that is critical for predicting the heat flux. The most commonly used heat source model is the Gaussian distribution [99]:

$$Q = \frac{2AP}{\pi r_0^2} e^{-2r^2/r_0^2}, \quad (2.6)$$

where A is the absorptivity of laser radiation, P is the laser power, r is the radial distance from the center of the laser beam and r_0 is the radius corresponding to the point where the laser intensity becomes $1/e^2$ of the maximum radiation. Fig. 2.2 shows the Gaussian contour of the heat flux at $P = 100$ W and $r_0 = 50$ μm . The Gaussian distribution of the heat flux with various laser powers and beam radiuses are demonstrated in Fig. 2.3.

Fig. 2.2. Gaussian contour of the heat flux with $P = 100$ W and $r_0 = 50$ μm .

Fig. 2.3. Gaussian distribution of the heat flux with different laser parameters: (a) powers and (b) beam radiuses.

Shi *et al.* [100] and Roberts *et al.* [101] used a hybrid model which combines the cylindrical model with the Gaussian model to predict the laser irradiation surface distribution. An equivalent cylindrical heat source is defined by a numerical average of the Gaussian heat flux distribution:

$$\bar{q} = \frac{Q_A}{S}, \quad (2.7)$$

where S is the surface area covered by the laser beam and Q_A is expressed by

$$Q_A = \int_S Q dS = AP \int_0^{r_0} \frac{2}{\pi r_0^2} e^{-2r^2/r_0^2} 2\pi r dr = AP(1 - e^{-2}). \quad (2.8)$$

The average cylindrical laser heat flux is obtained by substituting Eq. (2.8) into Eq. (2.7):

$$\bar{q} = \frac{AP}{\pi r_0^2} (1 - e^{-2}) = 0.864 \frac{AP}{\pi r_0^2}. \quad (2.9)$$

Additionally, the bell-like radial distribution is used to approximate the power energy density of the laser beam [67]:

$$Q = Q_{\max} \left(1 - \frac{r}{r_0}\right)^2 \left(1 + \frac{r}{r_0}\right)^2 \quad 0 < r < r_0, \quad (2.10)$$

$$Q_{\max} = \frac{3AP}{\pi r_0^2}. \quad (2.11)$$

As for most numerical models, the laser heat source is added to the top surface of the powder layer without the radiation penetration into the powder bed. This surface heat source may generate a considerably high temperature on the surface and shallow depth of the melt pool. Because of the porosity in the powder bed, the laser beam can be reflected, scattered and absorbed by powders. For that reason, the interaction between the laser beam and powders needs to be considered. A radiation transfer and thermal diffusion model developed by Gusarov *et al.* [67, 102] estimated the absorptivity of the volumetric heat source based on the assumption of slow consolidation. Streek *et al.* [103] provided a laser ray tracing algorithm to describe the heat energy distribution of the laser beam among the powders. The powder bed thickness and optimum thicknesses are estimated in the proposed algorithm. Rombouts *et al.* [104, 105] measured the effective extinction coefficients by a light-transmission technique and characterized the powder structure by quantitative image analysis of cross sections in the powder bed.

Foroozmehr *et al.* [106] used the optical penetration depth of the laser radiation to define a volumetric heat source:

$$H = (A \times P) / V, \quad V = S \times (\alpha \times OPD), \quad (2.12)$$

where α is a correction factor and OPD denotes the optical depth in the powder bed. C. Morsbach *et al.* [83] applied an interaction model of laser radiation and polymer powders. The model consists of two parts: the surface laser intensity distribution and transmitted

intensity of laser radiation which obeys an exponential decay as described by the Lambert-Beer-Law:

$$Q_v(x, y, z) = \frac{AQ}{\delta} \exp\left(-\frac{|z - z_s|}{\delta}\right), \quad (2.13)$$

where Q is the surface distribution of laser intensity, δ is the optical penetration depth and z_s is the position of the top surface of the powder layer.

For the powder-scale model, the powder bed is represented by individual powders. The ray tracing is a method to describe the laser absorption and reflection inside the powders and keyhole [107]. Yang *et al.* [5] modelled the laser interaction with powders based on the Fresnel absorption and multiple reflections and studied the effects of the particle size distribution on the melt flow. The modelling results suggested that the energy absorption of the powder bed is significantly larger than that of bulk materials. Tang *et al.* [26] adopted a simplified ray-track method to apply the heat energy to the metal/gas interface irradiated by the laser beam. Le *et al.* [108] simulated the fluid flow and heat transfer of the keyhole in the SLM process considering the multiple reflections of the laser beam based on the Fresnel absorption function.

2.3.1.2. Temperature field

Heat conduction is a primary heat transfer method to diffuse the laser heat in the powder bed and substrate in the SLM process. It is a simple and effective way to consider the heat conduction regardless of the laser radiation interaction with powders and fluid flow in the melt pool [11, 82, 109-111]. Therefore, enormous numerical models have focused on the heat conduction for analysing the temperature evolution in the SLM process. Zeng *et al.* [112] presented a dynamic mesh method for simulating the SLM process. The

dynamic meshing demonstrated higher computational efficiency than that of the conventional uniform mesh. Riedlbauer *et al.* [113] modelled the SLM process of PA12 and validated the temperature distribution and the melt pool size by the experiment. Li *et al.* [114] studied the effects of the scanning speed and laser power on the thermal behaviours and melt pool configuration during the SLM. The experiment was conducted to provide optimized processing parameters for reducing balling and pore formation. Zhang *et al.* [115] analyzed the influences of the process parameters including scanning speed, laser power, scan interval, and preheating process on the temperature distribution via a 3D FEM model.

Molten metals that are directly irradiated by the intensive laser beam can readily reach the vaporization point. The metal vaporization has an important influence on the temperature field because the latent heat of vaporization contains a large amount of heat energy. Verhaeghe *et al.* [45] used the finite volume method to model the SLM process which considered the metal vaporization, shrinkage of the powder bed and laser penetration in the powder bed and the modelling results showed that it was unrealistic to neglect metal vaporization. Wu *et al.* [32] proposed a 3D mesoscale CFD model for the SLM and investigated the effect of metal vaporization on the melt pool. By taking vaporization into account, the melt pool became deeper and narrower, and the surface temperature decreased. Furthermore, as the molten metals are vaporized, a recoil pressure would be applied on the melt pool surface. Under the effect of the recoil pressure, a keyhole may occur, which is of great importance for melt pool flow. The keyhole dynamic behaviors play a crucial role on the pore formation and surface morphology of the printed products. The recoil pressure

due to metal vaporization has been considered in numerous CFD models [31, 32, 34, 108, 116-118].

The metal vaporization and volume shrinkage due to the porosity among powders would give rise to the volume reduction of the powder bed [119]. A detailed multi-step algorithm for the multi-track and multi-layer SLM process considering the volume shrinkage and vaporization was discussed in [12]. The volumetric decrease is simulated by activating and deactivating elements by the element birth and death technique in ANSYS software. In addition, the FEM analysis which considered the volume shrinkage and vaporization has been proposed by Loh *et al.* [44]. In this developed model, the influences of the laser power and scanning speed on the melt pool configuration were carried out. Verhaeghe *et al.* [45] proposed an iterative shrinkage routine to take the volume shrinkage in the transition from powders to liquid into account.

Previous works mostly have focused on the single-track or single-layer process and scarce research on the multi-layer printing process has been reported. Roberts *et al.* [120] simulated the multi-layer process of SLM by building a 3D FEM model with the element birth and death technique in ANSYS. Hodge *et al.* [121] presented a multi-layer model which considered the radiation transportation in the powder bed as well as the powder-melt-solid phase transition. Their solutions gave temperature and the residual stress fields, and the thermal distortion of printed parts. Fu *et al.* [122] presented a 3D FEM model with a 2D Gaussian heat source to simulate a multi-layer SLM printing process and investigate the influences of the processing parameters and materials on the melt pool geometry. Tang *et al.* [26] simulated the single-track two-layer SLM process and investigated the formation of the interlayer defects. The results suggested that the interlayer defects caused by

suppressed wedding behaviors with the low deposited laser energy and surface roughness greatly affected the local thickness of the newly packed powder layer. Wang *et al.* [117] applied a fully coupled FVM and DEM approach to simulate a multi-track and multi-layer SLM process of Ti6Al4V and investigated the effects of the powder thickness and laser power on the porosity and melt track. Zhang *et al.* [77] developed a part-scale FEM model which can predict the temperature evolution of SLM-built complex geometry, as shown in Fig. 2.4.

Fig. 2.4. Temperature fields for an SLM-built product: (a) entire model, (b) workpiece with non-exposed powder and (c) workpiece with the substrate [77].

2.3.2. Mechanical behaviours

The research works on the mechanical behaviours in the SLM process have focused on the evolution of thermal stresses. Residual stresses that mainly arise from large thermal gradients in the layer-wise additive manufacturing process, as shown in Fig. 2.5. As the layer is irradiated by the high-intensity laser beam, a high temperature develops between the heated region and surround materials. In the heating process, the expansion of the heated material would be inhibited by the low-temperature material. Therefore, compressive stresses form in heated and surround materials. When the compressive

stresses are over the yield stress, plastic deformation would occur. As the laser beam moves away, the material scanned by the laser starts to cool down and the expanded materials start to shrink. The material with elastic deformation can shrink back to the original stage, while the plastically compressed materials cannot recover. As a result, tensile stresses develop in the layer newly printed part and compressive stresses are inside the surrounding part. Residual stresses may have a detrimental influence on dimensional accuracy and mechanical behaviours. To reduce such influence, numerical approaches can be employed to give insight into the evolution of residual stresses within the printed products during SLM and other manufacturing processes [123-128]. Among these numerical approaches, the sequentially coupling thermo-mechanical model that consists of a heat transfer analysis and a subsequent stress analysis has been prevalently employed [12, 121, 129, 130].

Fig. 2.5. Formation mechanism of residual stresses due to the temperature gradient [129].

The powder-liquid-solid phase transition and porous powder bed are important factors for analyzing the mechanical behaviour of SLM-built products. Generally, it is assumed that no stresses exist inside the molten metal. Hence, the Young's modulus and yield stresses are usually assigned with a relatively low value for molten metals. Li *et al.* [109] presented a 3D thermo-mechanical FEM model to study the stress evolution in the fusing process of the ceramic in consideration of the latent heat of melting and melt flow. The

Young's modulus and tangent modulus of molten ceramic materials were taken with relatively small values. In addition, before melted, the powder bed was not involved in the mechanical analysis. It is a common approach to deactivate the powder elements through the multiplication of their stiffness and a severe reduction coefficient. The element birth and death technique in the ANAYS has been widely used to handle the activation and deactivation of the powder elements [12, 42, 54]. Besides, the shrinkage of the powder bed can be considered by deactivating the shrunken elements [12].

It is of great significance in residual stress analysis to develop a reliable thermo-mechanical coupling model, which can provide a guide to study a variety of measures to minimize residual stresses such as process parameters optimization [131], pre-heating and post heat treatment [132-134], and scanning strategy [84, 110, 135]. Farahmand *et al* [136] used a 3D transient uncoupled thermo-elastic-plastic model to analyze the thermal phenomena and stress field in the cladding process, as shown in Fig. 2.6. According to the investigation of Mercelis *et al.* [129] for selective laser sintering and SLM, there existed large tensile residual stresses at the top and bottom of the product, while compressive stresses were in the middle of the product. Nickel *et al.* [137] modelled the deposition processes of SLM and showed the deposition pattern played a key role in the stress field and deflection of the printed product. Gusarov *et al.* [138] proposed a thermo-elastic model to evaluate residual stresses and concluded that the maximum residual stresses were along the scanning direction. In the coupled thermo-mechanical model presented by Zaeh *et al.* [84], the heat impact on the residual stresses and thermal deformations was studied. Li *et al.* [12] presented a multi-track and multi-layer model for predicting the residual stresses of the product manufactured by SLM, as shown in Fig. 2.7.

Fig. 2.6. Thermal stress fields for a single-track cladding process: (a) longitudinal stress component, (b) transverse stress component, (c) along-thickness stress component and (d) von Mises stress [136].

Fig. 2.7. Residual stress fields for a four-layer printed product with the removed substrate: (a) x-y plane, (b) y-z plane and (c) x-z plane [12].

Hussein *et al.* [41] used a 3D FEM model to investigate the evolution of thermal stress and temperature fields in the SLM process. The simulation results revealed that the cyclic heating and high cooling rates contribute to the large von Mises stresses in the printed product. Dai *et al.* [139] considered the influences of powder-liquid-solid phase transition through a thermo-mechanical FEM model and analyzed the transient temperature, thermal stresses and warpage of multi-material components. The simulation results revealed that the powder-to-solid transition leads to higher temperature transients, larger transient stresses and warpage. Mukherjee *et al.* [140] further improved the thermal model for residual stress analysis by considering the thermal-fluid effect in the melt pool. Ding *et al.* [141] studied the thermo-mechanical behaviours of a multi-layer wall fabricated by wire and arc 3D printing process and revealed that the previous layers have little effect on the following. Li *et al.* [142] developed a multi-scale methodology which integrates microscale, mesoscale and macroscale thermo-mechanical models.

Proper scanning strategy and preheating are favorable to the reduction of residual stresses. The experimental results conducted by Shiomi *et al.* [85] showed that heat treatment at 600 and 700 °C for one hour reduced residual stresses by about 70 %, and laser re-scanning and pre-heating of the powder bed decreased the tensile stress by 55% and 40 %, respectively. Kruth *et al.* [143] used a new method called the bridge curvature approach to measure the residual stresses. Their results found that the short scanning vectors and the preheating of the base plate can contribute to decreasing temperature gradient and consequently reducing thermal stresses. Yadroitsev *et al.* [130] studied the residual stress field of stainless steel 316L and Ti6Al4V. It was found that high thermal

gradients resulted in the high residual stress and preheating and scanning strategies are essential to decrease the thermal gradient.

Besides the thermal gradient, SSPT may lead to the formation of residual stresses, especially for the high carbon steel and Titanium alloy. As a two-phase titanium alloy (α and β phases), Ti6Al4V has attracted extensive attention for SLM owing to its high strength-to-weight ratio, excellent corrosion resistance and good stability in a wide range of working temperature [11, 131]. Metallurgical phase compositions of Ti6Al4V in SSPT are dependent on the temperature history and cooling rate. The phase transformation kinetics is governed by a diffusive transformation of the α to β phase in the heating process, while the transformation product of the β phase depends on the cooling rate in the cooling process. When the cooling rate drops below 20 K/s, the β phase decomposes back to the α phase [22, 144]. For a faster cooling rate, the diffusionless transformation process from the β phase to the martensite α' phase starts to occur. Furthermore, when the cooling rate is larger than 410 K/s, the β phase is fully transformed to the martensite phase α' [145]. Although the kinetics of SSPT for Ti6Al4V in the SLM process have been studied [55, 146, 147], the effect of SSPT on the thermal stress evolution have not been well understood.

2.3.3. Melt pool behaviours

In the SLM process, there exist complex multi-physics phenomena such as thermo-fluid dynamics which are influenced by many processing parameters and environmental factors. The thermo-fluid behaviours are of great significance for the microstructure and surface morphology of SLM-built products. The melt pool dynamics includes complex physical processes such as the thermal conduction, molten metal flow, laser radiation scattering and absorption, and phase transition (melting, vaporization and solidification),

as shown in Fig. 2.8.

Fig. 2.8. Thermo-fluid dynamics in the melt pool for the powder bed fusion process [3].

2.3.3.1. Fluid dynamics

To describe the melt flow behaviours, a multi-physics CFD model needs to be built [30, 148-153]. Generally, the CFD model in the melt pool is built based on the mass, momentum and energy conservation equations. The melt flow is mainly governed by the surface tension, gravitational force, recoil pressure, Marangoni convection and drag force in the mushy zone of the liquid and solid. Khairallah *et al.* [36] implemented a 3D powder-scale model to demonstrate the significance of the recoil pressure and Marangoni convection on the melt flow and emergence of the denudation, spattering and pore defects during the SLM process. Qiu *et al.* [24] studied the laser interaction with the powder bed by high-speed imaging camera and simulation, and concluded that the instability of the melt flow is mainly driven by the Marangoni force and recoil pressure, and unstable melt flow contributed to the pore formation and surface roughness. A finite volume model described by the continuity, momentum and energy equations was presented by Xiao *et al.* [154]. In this model, the volume shrinkage of the powder bed, buoyancy force and the

surface tension gradient were considered.

Fig. 2.9. Surface morphology of a single-track SLM process measured in the experiment (left) and simulated by the thermo-fluid model (right) for different powder thicknesses: (a) 20 μm, (b) 60 μm, (c) 80 μm, (d) 100 μm, the effects of (e) argon atmosphere and (f) scanning speed = 4000 mm/s [152].

In the CFD model, the individual powders can be taken into account [97, 155, 156]. Pei *et al* [157] used DED-based particle flow code (PFC) to simulate the packing process of rigid spherical powders of AlSi10Mg. The packing powder bed obtained from PFC software was then inputted into the CFD model. It was found that the intense Marangoni force was responsible for the instability in the melt pool, which led to the pore formation during SLM. Yan *et al.* [158] also applied the sequentially coupled DEM-CFD model to

investigate the single/multi-track defects such as the balling, discontinuous track and inter-track pores in the SLM process. To reduce the computational cost, the powder bed was assumed to be homogenous in some CFD models. Zhang *et al.* [159] presented a 3D multi-physics model to explore the characteristics of the melt flow for SLM of Ti6Al4V. They found that the vaporization may occur inside the melt pool, which would contribute to the pore formation of SLM-built products. Dai *et al.* [29] described the powder bed as the homogeneous model with effective material properties for analyzing the melt flow of SLM. Panwisawas *et al.* [152] applied a thermo-fluid model to predict the single laser scanning track and conducted a comparison between the simulated and measured surface morphology, as given in Fig. 2.9.

Few works have been conducted to explore the SLM process of metals under sub-atmospheric pressures. Such studies have mainly focused on the effect of ambient pressures on the interaction of vapor plume and powders [7, 56, 60], microstructure [61, 62] and surface morphology [62] in the SLM process. Matthews *et al.* [7] studied the denudation in the SLM process of metals under various ambient pressures. The results suggested that the ambient pressure affects the dominant force to move powders and thus change the denudated width of the melt track. Bidare *et al.* [56] studied the metal vapor interaction with powders and revealed that the reduction of the melt track profile was primarily ascribed to the increased denudation instead of metal vaporization. Masmoudi *et al.* [60] simulated the vapor flow under various ambient pressures and showed that low ambient pressure contributes to the expansion of metal vapor. Zhang *et al.* [61] found that the smooth surface of SLM-built products can be obtained by avoiding the balling phenomena in the vacuum condition.

SLM under sub-atmospheric pressures has two main advantages: improved penetration depth and porosity reduction due to the increased keyhole stability. The penetration depth improvement is ascribed to the reduced vaporization temperature and the shielding effect of the laser beam by vapor plume. The reduction of the vaporization temperature can lead to a decrease in the average temperature on the keyhole wall [160]. Therefore, less energy is needed to keep the keyhole open and deeper penetration depth can be achieved [57, 161] as the same laser energy is absorbed. It has been observed in the experiment [162] and simulation [60] that the sub-atmospheric pressure can lead to the expansion of vapor plume and the decrease of the plasma temperature. Consequently, the density and degree of ionization for the metal vapor are suppressed under sub-atmospheric pressures. For short laser wavelength used in laser welding and SLM, the Rayleigh or Mie scattering of the laser beam is a more dominant mechanism which is estimated to attenuate about 10 % of the incident laser radiation [57].

The software including the ANSYS Fluent, OpenFOAM, COMSOL and Flow3D have been widely used to investigate the behaviours of the fluid flow in the melt pool [33, 77, 153, 163-165]. Dai *et al.* [166] applied ANSYS Fluent to study the fluid behaviours of molten metals considering Marangoni force and gravitational force in the SLM process. The thermal gradient, velocity field, bubbles behaviour and the melt pool size were presented in their results. Gürtler *et al.* [167] used the OpenFOAM to simulate the transient 3D process of SLM. The model was based on the heat transfer equation, continuity equation and Navier-Stoke equation, and analyzes the influences of the powder bed thickness, laser power and scanning speed on the process dynamics.

2.3.3.2. Porosity

Pores are the major drawback of SLM-processed products [168, 169]. The crack propagation is usually induced by pores or the spots of the incomplete fusion. The mechanical test for TiAl6V4 products [170] showed that the micron-sized pores had a great effect on the fatigue strength. Lower scanning speed, re-melting, and higher preheating temperature can improve the density of components and these factors with mutual influences demonstrate non-linear features [171].

Fig. 2.10. Porosity in SLM-built products with different powder layer thicknesses: (a) 20 μm , (b) 60 μm and (c) 100 μm [24].

Many experiments have been conducted to investigate the effects of processing parameters on the porosity of printed products. The experiment conducted by Read *et al.*

[172] showed that the laser power, scanning speed and scan distance were important factors for the porosity evolution. It was found that an approximately critical energy density (60 J/m^3) can form the minimum pore fraction for AlSi10Mg. Li *et al.* [173] concluded that it can improve the surface morphology and product density to increase the laser power and decrease the scanning speed, layer thickness and hatch distance. Harrison *et al.* [174] found that laser power had the greatest influence on crack density. Gu *et al.* [175] achieved fully dense titanium components when a suitable processing window was properly determined. As given in Fig. 2.10, Qiu *et al.* [24] revealed that the powder layer thickness can significantly affect the porosity of printed products. Bauereiß [176] and Aboulkhair [177] found that increasing laser power and decreasing scanning speed can increase the melt pool size and thus reduce the porosity of printed products, as shown in Fig. 2.11.

Fig. 2.11. Porosity of SLM-built products affected by the laser factors: (a) laser power: (I) 90 W, (II) 120 W and (III) 180 W and (b) laser scanning speed: (I) 250 mm/s, (II) 500 mm/s, (III) 750 mm/s and (IV) 1000 mm/s [176, 177].

The CFD simulation has been widely used for predicting the porosity of SLM-built products [25, 26, 33, 108, 178, 179]. The keyhole collapse is one of the reasons that give rise to the pore formation, which can be summarized as the following steps: hump formation at the rear surface of the keyhole due to the capillary instability, bubble trapping at keyhole collapse, bubble flow backwards under the effect of the vortices flow and pore formation due to the bubble capturing of the solidification front. The keyhole-induced pores are found in the SLM process with excessive laser energy density [26]. On the other hand, the pore formation can be avoided if the trapped bubble escapes from the free surface of the melt pool.

Porosity is also caused by the lack of fusion, which leads to the formation of irregular pores at the interlayers. Such pores are induced due to the suppressed wetting behaviours as the laser energy is insufficient to fuse the substrate or previously printed layer. Irregular pores at the interlayer are generally found in the printed product with insufficient laser intensity due to low laser power or high scanning speed. Tan *et al.* [180] applied the coupled DEM-CFD model to investigate the influences of the laser deposited energy on the formation of porosity for SLM-built stainless steel 316L products. It was found that pores were distributed near to the top surface of the printed layer with low energy density, while the elliptical or spherical pores were located at the bottom of the keyhole as the high energy density is used. Bayat *et al.* [107] investigated the formation of pores induced by lack of fusion for the multi-track and multi-layer SLM process. They revealed that the improper fusion of powders contributes to the porosity between tracks.

The laser metal melting technology in the sub-atmospheric pressure has been paid more attentions due to its advantages such as the improved penetration depth, reduced

porosity and sound melt track [56-59]. It is well known that the sub-atmospheric pressures can benefit the porosity reduction by improving the keyhole stability. Li *et al.* [161] simulated the melt pool flow of laser welding under various sub-atmospheric pressures. The results revealed that more stable melt flow, weakened vortices and larger recoil pressure under the sub-atmospheric pressures facilitated the reduction of the bubble formation due to the keyhole collapse. The fast liquid flow due to larger recoil pressure is in favor of the bubbles escaping from the melt pool. Furthermore, the perturbation of laser energy absorption due to the shielding effect of metal vapor is suppressed under the sub-atmospheric pressures [181]. Jiang *et al.* [59] used a high-speed camera to capture the inner keyhole behaviour and concluded that the smaller fluctuation of the vapor pressure in the vacuum contributed to the stable keyhole wall.

2.4. Summary

A fundamental review of SLM is conducted. Also, two numerical approaches including FEM and CFD have been discussed. Furthermore, the current research advances on the SLM process analysis including the thermal, mechanical and melt flow behaviors have been investigated.

Chapter 3. Modelling Thermo-Mechanical Behaviors for Selective Laser Melting Additive Manufacturing of Stainless Steel 316L

To analyze the stress evolution in the SLM process, a thermo-mechanical coupling model is required. Although the thermo-mechanical model has been widely used in the additive manufacturing process, a multi-track and multi-layer model is still needed for simulating a practical manufacturing process. In this chapter, a 3D thermo-mechanical coupling model has been developed to simulate a multi-layer and multi-track SLM additive manufacturing process of stainless steel 316L. The model considers the temperature-dependent material properties and the powder melting, vaporization, solidification, cooling and shrinkage processes. The temperature and stress fields in the SLM process can be predicted by solving the heat conduction and stress equilibrium equations. The mechanical analysis considers the thermal expansion, elastic and plastic deformation during the heating and cooling processes.

3.1. Development of a thermo-mechanical model

In the SLM process, metal fusion occurs at the temperature above the melting point (1658 K for stainless steel 316L). After the laser moves away, the molten metal is solidified gradually. The heat provided by the laser beam diffuses through heat conduction and convection, air convection and radiation to the ambient environment. The thermal analysis model is developed based on the heat conduction equation which is the main way of heat diffusion. The thermo-mechanical model used in this research is sequentially coupled. Hence, the temperature field is evaluated from the thermal model and then input into the mechanical model to obtain the thermal deformation.

3.1.1. Heat conduction governing equations

A 3D transient heat conduction governing equation with temperature-dependent material properties and the volumetric heat source is expressed as [182]

$$\begin{cases} \rho \frac{\partial H}{\partial t} = \nabla \cdot [k \nabla (T)] + Q_v, t > 0 \\ T = T_0, t = 0 \end{cases}, \quad (3.1)$$

where T is the temperature at point (x, y, z) at time t ; ρ , H and k are the temperature-dependent density, enthalpy and thermal conductivity, respectively; Q_v is the volumetric heat source. The air natural convection and radiation to the ambient environment on the top surface of the powder bed can be expressed by [49]

$$-k \frac{\partial T}{\partial z} = \delta \omega (T^4 - T_\infty^4) + h_{\text{con}} (T - T_\infty), \quad (3.2)$$

where δ is the Stefan-Boltzmann constant taken as 5.67×10^{-8} , ω is the emissivity, T_∞ is the ambient temperature taken as 298 K and h_{con} is the heat convection coefficient ($\text{W}/(\text{m}^2 \cdot \text{K})$).

Other surfaces are assumed to be under thermal insulating boundary conditions:

$$(-k \nabla T) \cdot \vec{n} = 0, \quad (3.3)$$

where $\vec{n}$ is the normal vector of the boundary surface.

Fig. 3.1. Schematic of the laser penetration in the powder bed.

Fig. 3.2. Volumetric heat source with different optical depths λ : (a) dimensionless radiative flux and q (b) the derivative of dimensionless radiative flux $dq/d\xi$ [67].

Due to the porosity in the powder bed, the laser radiation can be absorbed or scattered by powders and the substrate as shown in Fig. 3.1. Based on the interaction model of the laser beam and powders, the volumetric heat source is expressed as [67]

$$Q_v(x, y, z) = -\gamma S_0(x, y) \frac{dq(\xi)}{d\xi}, \quad S_0(x, y) = \frac{2P}{\pi r_0^2} e^{-\frac{2(x^2+y^2)}{r_0^2}}, \quad (3.4)$$

where γ is the extinction coefficient, the dimensionless coordinate $\xi = \gamma z$ denotes the extinction thickness along the z direction, $S_0(x, y)$ is the Gauss surface distribution of the laser energy, P is the laser power, r_0 is the laser beam radius and the normalized laser radiative flux q is given by solving the radiation transfer equation in the thickness direction of the powder bed [67]:

$$q(\xi) = \frac{\eta a}{(4\eta - 3)D} \{ (1 - \eta^2) e^{-\lambda} [(1 - a) e^{-2a\xi} + (1 + a) e^{2a\xi}] - (3 + \eta e^{-2\lambda}) \times [1 + a - \eta(1 - a)] e^{2a(\lambda - \xi)} + [1 - a - \eta(1 + a)] e^{2a(\lambda - \xi)} \} - \frac{3(1 - \eta)(e^{-\xi} - \eta e^{\xi - 2\lambda})}{4\eta - 3}, \quad (3.5)$$

$$D = (1 - a)[1 - a - \eta(1 + a)] e^{-2a\lambda} - (1 + a)[1 + a - \eta(1 - a)] e^{2a\lambda}, \quad (3.6)$$

where η is the hemispherical reflectivity of powders, $a = \sqrt{1 - \eta}$ and the optical thickness $\lambda = \gamma L$ with L being the thickness of the powder layer. Fig. 3.2 shows the distribution of dimensionless radiative flux and the volumetric heat source along the thickness of the powder bed from Eq. (3.5).

3.1.2. Stress analysis model

In the stress analysis model, the thermal, elastic and plastic deformations are taken into consideration. Hence, the total strain increment $d\epsilon^{\text{total}}$ is expressed by

$$d\epsilon^{\text{total}} = d\epsilon^e + d\epsilon^p + d\epsilon^{\text{th}}, \quad (3.7)$$

where $d\epsilon^e$ is the elastic strain increment, $d\epsilon^p$ is the plastic strain increment and $d\epsilon^{\text{th}}$ is the thermal strain increment. Plastic strain increment is given by considering the flow plasticity:

$$d\epsilon^p = d\lambda \left\{ \frac{\partial f}{\partial \sigma} \right\}, \quad (3.8)$$

where λ is the hardening parameter, σ is the stress tensor and the yield function f is obtained by the von Mises yield criterion:

$$f = \sigma_{\text{von}} - \sigma_{\text{yield}}, \quad (3.9)$$

where σ_{yield} and σ_{von} are the yield stress and von Mises stress, respectively. The thermal strain increment is expressed as

$$d\epsilon^{\text{th}} = \alpha_T \Delta T, \quad (3.10)$$

where α_T is the coefficient of thermal expansion.

3.1.3. Volume shrinkage of the powder bed

The volume of the powder bed is reduced after the powders are melted by the laser beam. The volume decreasing results from the volume shrinkage of the porous powder layer and vaporization. The vaporized metal can be neglected compared with the volume shrinkage. Hence, the volumetric shrinkage is 40% of the powder bed volume because the porosity of the powder bed is assumed to be 0.4 [183]. A detailed multi-step algorithm provided in [12] is used in our work to consider the volume shrinkage of the powder bed. The volume decrease is simulated by activating and deactivating elements by the element birth and death technique in ANSYS software. After the volume of the powder bed is reduced, the top surface of the printed product is assumed to be flat. The thermophysical properties of the materials including the density, heat conductivity and enthalpy are associated with the volume shrinkage. The material properties are changed from powders to the bulk material as the temperature is over the melting point. Since the powders have a negligible effect on the mechanical analysis of the printed product, the elements of fresh powders are deactivated in the mechanical model.

3.1.4. Material properties

The temperature-dependent thermophysical properties including thermal conductivity, density, and specific heat capacity (or enthalpy) are used. The thermal conductivity k_p in the powder bed is assumed to be 0.3 W/(m·K) before melting [105, 184]. The density of the powder bed is given as

$$\rho_p = \begin{cases} (1-\phi)\rho_s & T < T_m \\ \rho_s & T > T_m \end{cases}, \quad (3.11)$$

where the porosity φ is assumed to be 0.4 [1], ρ_s is the density of the bulk stainless steel 316L and T_m is the melting temperature.

Fig. 3.3. Thermophysical properties of the stainless steel 316L bulk and powder bed: (a) density [185], (b) heat conductivity [184, 185] and (c) enthalpy [185].

To take into account the melting and solidification processes, a large value converted from the latent heat during the phase change process is added to the specific heat capacity. Consequently, the temperature-dependent specific heat capacity has a sharp peak at the melting point, which may cause the convergence problem in the numerical simulation. Therefore, the enthalpy is used as the input parameters of the material property instead of specific heat capacity. The enthalpy is expressed as

$$H = \begin{cases} H_s & T < T_m \\ H_s + H_m & T_m \leq T < T_v \\ H_s + H_m + H_v & T_v \leq T \end{cases}, \quad (3.12)$$

where T_m is the melting point, T_v is the boiling point, H_m is the latent heat of melting, H_v is the latent heat of vaporization and H_s is the sensible heat defined as

$$H_s = \int_{25^\circ\text{C}}^T \rho \cdot C dT, \quad (3.13)$$

where C is the specific heat capacity. The temperature-dependent material properties of the powder bed and bulk metals are depicted in Fig. 3.3.

Fig. 3.4. Mechanical properties of stainless steel 316L: (a) the Young's modulus [130], (b) thermal expansion [130], (c) yield stress [130] and (d) tangent modulus [121].

The temperature-dependent mechanical properties of stainless steel 316L [121, 130] including the Young's modulus, coefficient of thermal expansion, yield stress and tangent modulus for the mechanical analysis are plotted in Fig. 3.4. A bilinear kinematic hardening constitutive model is used for the plastic deformation as shown in Fig. 3.5. The model requires the initial yield stress σ_0 and tangent modulus E_T as input parameters. The tangent modulus is assumed to be about 1/20 of the Young's modulus [186]. To avoid divergence problem, the Young's modulus of metals in the melt pool is taken as a relatively smaller value instead of zero. The Young's modulus in the molten metal (above 1658 K) is set to be 10^6 Pa, which is much smaller than the value in the room temperature because the thermal deformation is neglected in the liquid metal.

Fig. 3.5. A bilinear constitutive model for stainless steel 316L [121, 130].

3.2. Thermal analysis

In this section, the thermal model is verified through the comparison between the modelling melt pool sizes with the experimental results. Subsequently, the effects of the

laser power and scanning speed on the temperature field are analyzed. Furthermore, the evolution of the temperature field for a multi-track and multi-layer printing process is investigated.

3.2.1. Validation of the thermal model

The modelling parameters for the SLM process of stainless steel 316L used in this simulation are listed in Table 3.1. The FEM model for the substrate with a size of $2 \times 1 \times 0.2 \text{ mm}^3$ and the powder layer with a size of $1 \times 0.5 \times 0.05 \text{ mm}^3$ are depicted in Fig. 3.6. The temperature field for the one-track laser melting process is illustrated in Fig. 3.7. The thermal model is validated by comparing the cross-section width of the melt pool with experimental results [67]. The liquid/solid phase distribution at the cross section of the melt pool is demonstrated in Fig 3.8. The width of the melt pool with different scanning speeds is illustrated in Fig. 3.9. A good match with the experimental results is achieved, especially for the high scanning speed.

Table 3.1. Processing parameters for stainless steel 316L used in this simulation [185, 187-189].

Parameter	Value
Powder layer thickness h (μm)	50
Laser beam radius r_0 (μm)	60
Laser power P (W)	20-100
Laser scanning speed v (mm/s)	100-800
Environment temperature T_∞ (K)	298
Latent heat of melting H_m (kJ/kg)	270
Latent heat of vaporization H_v (kJ/kg)	7450
Natural convection coefficient h_{con} ($\text{W}/(\text{m}^2 \cdot \text{K})$)	100

Fig. 3.6. Mesh of the FEM model for the one-track SLM process.

Fig. 3.7. Temperature fields of a one-track printed product with $P = 80$ W and $v = 500$ mm/s.

Fig. 3.8. Liquid/solid phase distributions in the cross section of the melt track with different scanning speeds ($P = 45$ W): (a) 120 mm/s, (b) 160 mm/s, (c) 200 mm/s and (d) 240 mm/s.

Fig. 3.9. Comparison between the simulation and experiment for the melt pool width with different scanning speeds [67].

3.2.2. One-track process

In the simulation, the scanning speed is 500 mm/s and other parameters are listed in Table 3.1. Figs. 3.10 and 3.11 give the temperature distribution on the x - y and x - z planes with different laser powers, respectively. The temperature level increases with the elevating laser power. Consequently, the penetration depth, width and length of the melt pool also increase. As the laser power $P = 40$ W, the depth of the melt pool ($T > 1658$ K) is smaller than the thickness of the powder layer. Hence, the powder bed is not fully melted. As the laser power is over 80 W, the melt pool can penetrate through the powder bed into the substrate. As a result, the newly printed layer can be bonded to the substrate.

Fig. 3.10. Temperature fields (unit: K) on the x - y plane with different laser powers: (a) 40 W, (b) 60 W, (c) 80 W and (d) 100W.

Fig. 3.11. Temperature fields (unit: K) on the x - z cross section of the melt track with different laser powers: (a) 40 W, (b) 60 W, (c) 80 W and (d) 100 W.

Fig. 3.12. Temperature fields (unit: K) on the x - y plane with different scanning speeds ($P = 45$ W): (a) 200 mm/s, (b) 400 mm/s, (c) 600 mm/s and (d) 800 mm/s.

Fig. 3.13. Temperature fields (unit: K) on the x - z cross section of the melt track with different scanning speeds ($P = 45$ W): (a) 200 mm/s, (b) 400 mm/s, (c) 600 mm/s and (d) 800 mm/s.

Figs. 3.12 and 3.13 show the temperature fields on the x - y and x - z planes with different scanning speeds, respectively. The increase of the scanning speed leads to the temperature reduction. Consequentially, the size of the melt pool decreases with increasing scanning speed. At $v = 200$ mm/s, the melt pool is wide and deep. Meanwhile, the maximum temperature is lower than the vaporization temperature (3090 K). Hence, the metal vaporization can be avoided. As the scanning speed increases to 400 mm/s, the powder bed cannot be fully melted. Fig. 3.14 illustrates the effects of the laser power and scanning speed on the melt pool size. The width, depth and length of the melt pool reduce as the scanning speed increases. The influence of the higher scanning speed on the melt pool size is not noticeable. Fig. 3.14 (b) shows that increasing laser power can lead to a significant increase in the melt pool size.

Fig. 3.14. Size of the melt pool via (a) scanning speed and (b) laser power.

Fig. 3.15. Temperature distributions with different laser powers in the (a) x and (b) y directions.

The temperature distributions on the top of the powder layer with different laser powers along the x and y directions are demonstrated in Fig. 3.15. The point with the maximum temperature is located at the center of the melt pool. The temperature is distributed symmetrically along the x direction. The maximum temperature and high-temperature zone increase with increasing laser power. The temperature declines dramatically at $y = 100$ μm because the powder layer with low heat conductivity cannot

diffuse the heat to neighboring powders within a short time. As the maximum temperature is around the vaporization temperature, the temperature increase with the rising laser power is not noticeable, which is consistent with the increase of the melt pool size, as shown in Fig. 3.14 (b). It can be explained that a large amount of heat energy is absorbed due to the large latent heat of vaporization (7450 kJ/kg) while the temperature of molten metal reaches the vaporization point with the increasing laser power. The temperature of the molten metals gradually decreases in the opposite direction of laser scanning because the heat diffuses through the heat conduction within the substrate, and heat radiation and air convection with the ambient environment. Similar to the temperature distribution in the x direction, a dramatic temperature increase occurs between the molten pool and powders along the y direction. Figs. 3.16 (a) and (b) illustrate the temperature distribution along the x and y directions with various scanning speeds, respectively. The temperature decreases with the increase of the scanning speed. It is observed from the temperature distribution in the y direction that the temperature in the beginning region tends to have a similar value.

Fig. 3.16. Temperature distributions with different scanning speeds in the (a) x and (b) y directions.

Fig. 3.17. Effects of (a) laser powers and (b) scanning speeds on the temperature during the printing and cooling processes.

Fig. 3.17 shows the temperature evolution with different laser powers and scanning speeds. The point for observing the temperature is located at the point (0.5, 0.6, 0.25). When the laser reaches the point, the temperature increases from the initial temperature of 298 K to the maximum temperature around 0.4 ms. A high heating rate occurs during the heating process. After the laser moves away, the temperature drops quickly at the beginning of the cooling process and then gradually reduces. The point with higher laser powers ($P = 80$ and 100 W) can maintain a maximum temperature for a while after the laser moves away, while a sharp change occurs at the peak temperature for the lower powers ($P = 60$ and 40 W). This can be explained that a large amount of heat energy absorbed from the laser cannot be diffused in a short time and meanwhile the large latent heat of vaporization prevents the increase of the temperature.

3.2.3. Multi-track process

In this case, the temperature distribution of a multi-track and one-layer printing process is investigated. The FEM model is illustrated in Fig. 3.18. Table 3.2 lists the

modelling parameters for the multi-track process. A powder bed with the thickness $h = 50$ μm is spread over the top of the substrate. A fine mesh is used for the printed product and the central region of the substrate. The scanning strategy is shown in Fig. 3.19 for the multi-track and one-layer printing process. The size of the printed product is $0.5 \times 0.5 \times 0.05$ mm^3 . The scanning hatch distance between two tracks is set to be 1.2 times of the laser beam radius r_0 . The one-layer printed product can be covered with eight tracks according to the size in the scanning pattern as given in Fig. 3.19.

Table 3.2. Modelling parameters for a multi-track printing process.

Parameter	Value
Powder layer thickness h (μm)	50
Laser beam diameter d (μm)	120
Laser power P (W)	100
Laser scanning speed v (mm/s)	600
Environment temperature T_∞ (K)	298
Substrate $L \times W \times H$ (mm^3)	$1 \times 1 \times 0.3$

Fig. 3.18. Mesh of the FEM model for a multi-track SLM process.

Fig. 3.19. Scanning strategy for a multi-track SLM process.

Fig. 3.20. Evolution of the temperature field during a multi-track printing process: (a) $t = 0.521$ ms, (b) $t = 0.958$ ms, (c) $t = 3.833$ ms and (d) $t = 7.708$ ms.

The evolution of the temperature field for a multi-track printing process is shown in Fig. 3.20. Half of the track is scanned by the laser beam as depicted in Fig. 3.20 (a). After scanned, the powder bed is found to shrink due to the powder-liquid transition. The shrunken material is assumed to be 40 % of the powder bed volume. The molten metal

starts to solidify after the laser leaves. Since the bulk material has around 100 times larger density than that of the powder bed [67], the heat diffuses fast in the printed product while the temperature of the surrounding powders remains constant at the initial value.

To further observe the temperature evolution at different positions, the temperature evolution at five points on the surface of the powder bed (A , B , C , D , and O as shown in Fig. 3.19) are demonstrated in Fig. 3.21. The temperature rises sharply when the laser reaches the point. After the laser leaves, the temperature firstly decreases fast at the beginning of the cooling process and slowly approaches a constant value. This variation trend at each point for the multi-track process is similar to the one-track result. Two temperature peaks are observed in the temperature variation, which is caused by the preheating at points O and D , and reheating at point A . The peak temperature due to reheating and preheating is lower than the peak value at the point which is irradiated by the laser beam. The time difference between the two peaks is the duration of printing one track.

Fig. 3.21. Temperature history at different positions during a multi-track printing process.

3.2.4. Multi-layer process

The mesh of the FEM model and scanning pattern for a multi-layer SLM process are shown in Fig. 3.22. A fine mesh is used for the four-layer printed product and the heat-affected central zone of the substrate. A unidirectional scanning pattern is adopted within one layer and the scanning directions between the two layers are orthogonal. The cooling time is 0.01 s after one layer of powders is melted by the laser beam.

Fig. 3.22. (a) Mesh of the FEM model and (b) scanning pattern for a multi-layer SLM process.

Fig. 3.23 demonstrates the evolution of the temperature field for an eight-track four-layer SLM process. Fig. 3.23 (a) shows the temperature field after one layer of powders is scanned. The scanning is performed along the y direction and the scanning sequence of tracks is along the x direction. The minimum temperature takes place at the corners of the first track and the maximum temperature is at the ending position of the eighth track. The temperature field of a two-layer printed product is given in Fig. 3.23 (b). The scanning direction, which is orthogonal to that of the first layer, is along the y direction. The temperature fields for three-layer and four-layer products are illustrated in Figs. 3.23 (c) and (d), respectively. After cooling down of 0.001 s, the temperature of the four-layer

product drops, while the positions of the minimum and maximum temperature remain unchanged. As the printed product cools down for 500 s, the temperature of the entire printed product approximates to the environment temperature 298 K.

Fig. 3.23. Evolution of the temperature field for an eight-track four-layer SLM process: (a) $t = 0.007708$ s, (b) $t = 0.025375$ s, (c) $t = 0.043042$ s, (d) $t = 0.060708$ s, (e) $t = 0.061667$ s and (f) $t = 500.061$ s.

3.3. Stress analysis

The effect of the structural distortion on the thermal behaviours is neglected. Hence, a sequentially thermo-mechanical coupling model is used to evaluate the thermal stress evolution in the SLM process. The temperature field evaluated from the thermal analysis is input into a mechanical model to evaluate the thermal deformation. Furthermore, an elastoplastic bilinear constitutive model is employed for mechanical analysis. Based on the thermo-mechanical model, the stress field for a multi-track and multi-layer printed product is investigated.

3.3.1. One-track process

The directions of stress components are shown in Fig. 3.24. The stress component σ_y , σ_x and σ_z are along the scanning direction, along the direction perpendicular to the scanning direction in the printing plane and normal to the scanning plane, respectively. The temperature field for the one-track printing process is illustrated in Fig. 3.25. The time for scanning one track is 1.0225 ms. The melt track is formed after the laser scans the powder layer. In the following stress analysis, only the printed track is demonstrated.

Fig. 3.24. Schematic of stress component directions.

Fig. 3.25. Temperature field for a one-track printed product at $t = 1.0225$ ms.

Fig. 3.26. Stress fields for a one-track printed product at $t = 1.0225$ ms: (a) σ_x , (b) σ_y , (c) σ_z and (d) von Mises stress.

The contours for stress components in the x , y and z directions and the von Mises stress at $t = 1.0225$ ms are shown in Fig. 3.26. The minimum stress is in the region of the melt pool. To neglect the thermal deformation in the melt pool, the Young's modulus is set to be a small value (10^6 Pa). The von Mises stress decreases along the y direction. The compressive stress occurs around the melt pool because neighboring materials constrain the thermal expansion of the high-temperature melt pool. After the laser beam moves away, the metal starts to contract due to the heat diffusion and then the stress component becomes tensile stress. The maximum residual stresses occur at the bottom of the printed product due to the constraints of the substrate.

Fig. 3.27. Residual stress fields for a one-track printed product at $t = 100$ s: (a) σ_x , (b) σ_y , (c) σ_z and (d) von Mises stress.

The residual stress field for the one-track process after cooling down is shown in Fig. 3.27. In the cooling process, the heat in the printed product is diffused through the heat conduction to the substrate, and air convection and radiation to the ambient environment. The cooling process is 100 s from the end of scanning. Finally, the printed product and the substrate are approximate to the environment temperature $T_{\infty} = 25^{\circ}\text{C}$. The large von Mises stress occurs along the centerline of the printed track. The stress component σ_y is larger than the other two stress components because the newly scanning metal is constrained by the previously printed product in the scanning direction. The compressive stresses are observed to exist at each corner due to the stress balance with the tensile stresses at the centerline.

The evolution for temperature and stress fields for the one-track printing process is shown in Figs. 3.28 and 3.29, respectively. The position for examining the stress evolution is located at the center of the melt track. As the laser reaches the point, the powder layer absorbs a large amount of heat and the temperature increases sharply, as shown in Fig. 3.28. As a result, the metal begins to expand, but the expansion is restricted by the low-temperature surrounding materials. Therefore, the high-temperature material experiences sharp compressive stresses. With increasing temperature, the compressive stress increases to the yield stress. When the temperature reaches the melting point, the stiffness of the molten material is extremely small and thus the stresses are low. After the laser moves away, the metal starts to contract during the cooling process. The metal with the stress lower than the yield stress starts to recover but is constrained by the product with plastic deformation, which causes the formation of residual tensile stresses. The stress components

in the x and z directions tend to a relatively small value with the time because the constraint is small in these two directions.

Fig. 3.28. Temperature history during a one-track printing process.

Fig. 3.29. Evolution of stresses during a one-track printing process: (a) σ_x , (b) σ_y , (c) σ_z and (d) von Mises stress σ_{von} .

3.3.2. Multi-track process

Fig. 3.30 illustrates the distributions of stress components σ_x , σ_y and σ_z , and von Mises stress σ_{von} for a multi-track printed product. The stress distributions are at $t = 0.007667$ s when one layer of powders is scanned. The scanning strategy is shown in Fig. 3.19. The stress components σ_y and σ_z have the maximum and minimum stresses, respectively. This can be explained that the newly printed material in the scanning direction (y direction) is always constrained by the previously printed product, while the constraints in the z direction are free. The stress components σ_x , σ_y , and σ_z in the region of melt pool are small because the Young's modulus of the molten metal is assumed to be a low value. Compressive stress components occur near to the edge of the melt pool because the expansion of the high-temperature melt pool is constrained by low-temperature surrounding materials. The maximum von Mises stress occurs at the first track and the von Mises decreases from the previously printed track to newly printed one.

Fig. 3.30. Stress fields for a multi-track printed product at $t = 8.36$ ms: (a) σ_x , (b) σ_y , (c) σ_z and (d) von Mises stress σ_{von} .

Fig. 3.31. Evolution of temperature and von Mises stress fields for a one-layer product in the cooling process: (a) $t = 0.008667$, (b) $t = 0.012667$ s, $t = 0.017667$ s and $t = 100.008$ s.

The evolution of temperature and von Mises stress fields for a multi-track printed product in the cooling process is shown in Fig. 3.31. The heat diffusion during cooling is mainly through the radiation and natural air convection on the top surface of the printed product and the heat conduction to the ambient fresh powders and the substrate. At the beginning of the cooling process, the high temperature is located at the ending region of the scanning. In the cooling process of the printed product, the heat diffusion results in the reduction of the temperature gradient. Consequently, the high-temperature region moves to the center of the printed product. Finally, the temperature of the printed product becomes uniform. The von Mises stress in the high-temperature region changes from the compressive stresses to tensile stresses and increases during the cooling process. In general, the newly printed track has more large stress regions than the previously printed track.

The evolution of stress components in the x and y directions is shown in Fig. 3.32. At the beginning of the cooling process, the compressive stress component σ_x disappears as the molten metals are solidified. The maximum stress component σ_x increases gradually during the cooling process and finally approaches the yield stress of stainless steel 316L. When the temperature of the printed product becomes uniform, as shown in Fig. 3.31 (d), the maximum stress component σ_x is distributed near the center of the printed product. At the beginning of the cooling process, the maximum stress component in the y direction is located at the region of the first track. Similar to the stress component σ_x , the maximum stress component σ_y increases in the cooling process. Furthermore, large stress component σ_y is distributed along the scanning track.

Fig. 3.32. Evolution of stress components in the x and y directions for a one-layer printed product during the cooling process: (a) $t = 0.008667$, (b) $t = 0.012667$ s, (c) $t = 0.017667$ s and (d) $t = 100.008$ s.

Fig. 3.33 shows the distributions of the residual stress components σ_x , σ_y , and σ_z , and von Mises residual stresses after the printed product are cooled down. Because of the free constraints, the stress component in the z direction has the minimum stress compared to

the other two stress components. Also, the stress component σ_y is larger than the other two stress components. Additionally, the von Mises stresses have a similar distribution with the stress component σ_y which has large value along the scanning tracks which undergoes high-temperature gradients during the printing process.

Fig. 3.33. Residual stress fields for a multi-track printed product at $t = 500.008$ s: (a) σ_x , (b) σ_y , (c) σ_z and (d) von Mises stress σ_{von} .

Fig. 3.34 shows the history of the von Mises stress during the printing and cooling processes at different positions. The positions of points A , O and D are indicated in Fig. 3.19. As compared with the temperature history at points A , O and D as shown in Fig. 3.21, a sharp peak of the von Mises stress occurs when the laser is scanning the examined position. This can be explained that the fast expansion of the heated material results in a rapid increase of the stress. As the metals are melted, the low Young's modulus of the molten material contributes to the dramatic decrease of the stress. In the printing process,

the von Mises stress at point A rises with fluctuation because the point A experiences cyclic reheating processes. After the laser moves away, the von Mises stress increases gradually during the cooling process. The stress at the corner point D is larger than the stresses at points A and O because the ending region of the laser scanning has a more significant temperature decrease during the cooling process as well as is constrained by the previously printed product.

Fig. 3.34. History of von Mises stresses during a multi-track printing process at different positions.

3.3.3. Multi-layer process

In this case, the stress field for a four-layer printed product is analyzed. The mesh of the FEM model and the scanning pattern for the four-layer printed product are shown in Fig. 3.22. The evolution of stresses in the product during the printing process will be presented.

Fig. 3.35. Evolution of temperature and von Mises stress fields for a four-layer printed product during the cooling process with $P = 80$ W and $v = 0.5$ m/s: (a) $t = 0.060667$ s, (b) $t = 0.061667$ s, (c) $t = 0.070667$ s and (d) $t = 1.60667$ s.

Fig. 3.35 demonstrates the evolution of the temperature and von Mises stress fields for a four-printed product during the cooling process. The temperature level of the four-layer product decreases with the cooling time and tends to a uniform temperature. At the beginning of the cooling process, the maximum temperature of the printed product decreases from 3165 K to 1728 K within the cooling time of 0.001 s. The printed product undergoes a dramatic temperature change process in which the cooling rate is around 10^6 K/s. After the cooling time of 1 s, the printed product reaches a uniform temperature around 450 K. For the evolution of the von Mises stress, low stresses are found in the high-temperature region, especially for the molten metals. Furthermore, high stresses are distributed on the corners at the bottom of the printed product. The maximum stress slightly reduces from 138 to 123 MPa in 0.001 s, but after that, it increases gradually with the cooling time. After the cooling time of 1 s, the maximum stress (296 MPa) approximates to the yield stress of stainless steel 316L (280 MPa). Large von Mises stresses observed on the top of the printed product distributed along the scanning track.

Fig. 3.36 shows the residual stress distribution of a four-layer printed product after the cooling process. For stress component σ_x , the maximum stress is located at the second layer because the scanning direction of the second layer is along the x direction and the printed metals are subject to the strong constraint of the first layer and substrate. The stresses tend to decrease and change from tensile stresses to compressive stresses along the thickness direction. In addition, the stress component σ_y have a similar distribution with the stress component σ_x , but the maximum stresses are distributed in the first layer which is scanned by the laser beam in the y direction and constrained by the substrate. The stress level along the thickness direction (z direction) is smaller than the other two stress

components because of fewer constraints in the scanning process.

Fig. 3.36. Residual stress fields for a four-layer printed product with $P = 80$ W and $\nu = 0.5$ m/s: (a) σ_x , (b) σ_y , (c) σ_z and (d) von Mises stress σ_{von} .

From the distributions of the residual stresses in the cross section of the four-layer printed product in Fig. 3.36, large residual stress components are located near the bottom region of the printed product. The maximum von Mises residual stresses, which are ascribed to the large compressive stress components, are located at the top surface of the printed product. Additionally, the first and second layers also demonstrate large von Mises stresses due to the strong constraints from the substrate.

3.4. Summary

A 3D thermo-mechanical model with temperature-dependent material properties based on FEM has been developed for simulating a multi-track and multi-layer SLM process. The simulation results reveal that both increasing laser power and scanning speed contribute to the increase of the melt pool size. The modelling temperature field is symmetric on the plane perpendicular to the scanning direction. Additionally, there exists a sharp temperature gradient between melted metals and surrounding powders due to the low heat conductivity of the powder bed. For the one-track printing process, after the printed product cools down, large von Mises residual stresses occur around the centerline of the melt track. The stress component along the scanning direction is larger than the other two stress components. For the multi-track process, large von Mises residual stresses are distributed along the scanning tracks. In addition, large residual stresses are distributed in the first two layers of the multi-layer printed product due to the constraints from the substrate.

Chapter 4. Modelling Thermo-Metallurgical-Mechanical Behaviours for Selective Laser Melting Additive Manufacturing of Ti6Al4V

For Ti6Al4V or high carbon steels, the metallurgical phase transformation has an important effect on the mechanical behaviors of printed products. However, the phase transformation has not yet been considered for the stress analysis in the SLM-built product. In addition, a cyclic phase transformation would occur in a multi-track and multi-layer SLM process, which increases the complexity and significance of the phase transformation. Therefore, a thermo-metallurgical-mechanical coupling model with the consideration of SSPT has developed to simulate the SLM additive manufacturing process of Ti6Al4V. The developed model considers the metallurgical SSPT and the powder-liquid-solid transition. Furthermore, the thermophysical and mechanical properties associated with the temperature and metallurgical compositions are taken into account. The objective of this work is to investigate the effects of SSPT on the residual stress evolution within the product during the SLM process of Ti6Al4V.

4.1. Development of a thermo-metallurgical-mechanical model

This section introduces how to develop the thermo-metallurgical-mechanical coupling model for the SLM process. In the coupling procedure, the temperature field is first obtained by solving transient heat conduction and then used to evaluate the metallurgical solid-phase field. Subsequently, the temperature and solid-state phase fields are imported into the mechanical analysis model to obtain thermal strain and volumetric change strain, respectively. A detailed coupling procedure is illustrated by the flowchart in Fig. 4.1. The coupling model is developed based on FEM in ANSYS parametric design language.

Fig. 4.1. Flowchart of a thermo-metallurgical-mechanical model for the SLM process.

The heat absorbed from the laser beam diffuses through heat conduction in the powder bed and substrate, heat convection in the melt pool, and air convection and radiation with the ambient environment. The thermal model is based on the heat conduction governing equation because heat conduction occupies the main role in the instantaneous diffusion of heat. The air convection and radiation are considered as boundary conditions. A volumetric heat source is used to represent the interaction between the laser beam and powders due to the porosity of the powder bed. The volume fractions of solid-state phases during the heating and cooling processes are predicted by semi-empirical formulas which are a

function of the temperature. The temperature and solid-state phase fields are imported into the mechanical analysis model to obtain the thermal strain and volumetric change strain, respectively. Finally, an elastoplastic constitutive relationship considering the thermal strain and volumetric change strain is proposed to evaluate stress fields.

4.1.1. Thermo-metallurgical model

Ti6Al4V with both α and β phases may undergo SSPT during the heating and cooling processes. A schematic of the volumetric change due to SSPT during the heating and cooling processes is given in Fig. 4.2. When the temperature reaches the transformation temperature B_s during the heating process, the α phase starts to be transformed to the β phase. Since the unit cell volume of the β phase is larger than that of the α phase, the material volume decreases with the increasing temperature. During the cooling process, when the temperature is lower than the transformation temperature M_s , the material volume increases in the phase transformation of the β to α (or α').

Fig. 4.2. Schematic of the volumetric change due to SSPT [52].

During the heating process, the volume fraction of the β phase is given by the Castro model [190]:

$$f_{\beta}(T) = \begin{cases} 0.075 + 0.92 \exp(-0.0085(B_f - T)), & 298 \text{ K} < T < B_f \\ 1, & T > B_f \end{cases}, \quad (4.1)$$

where the finish temperature B_f of the α to β phase transformation is 1253 K. Due to the high cooling rate in the SLM process, the diffusive transformation from the β phase to the α phase is suppressed and the β phase is completely converted to the martensite α' phase [144, 191]. The volume fraction of the martensite α' phase is obtained by the Koistinen-Marburger equation [192]:

$$f_{\alpha'}(T) = \begin{cases} f_{\beta}' (1 - \exp(-0.015(M_s - T))), & T < M_s \\ 0, & T > M_s \end{cases}, \quad (4.2)$$

where f_{β}' is the initial volume fraction of the β phase in the martensite transformation and the transformation temperature M_s is set to be 923 K in this model. Phase volume fractions during heating and cooling processes are given in Fig. 4.3.

Fig. 4.3. Phase volume fractions during (a) heating process [190] and (b) cooling process [192].

A schematic of SSPT in the multi-track and multi-layer SLM process is shown in Fig. 4.4. A layer of powders is initially spread on the substrate (see Fig. 4.4 (a)). The main phase composition of the powder and substrate is the α phase. In (b), when the first track is scanned by the laser beam, the heat-affected zone of the powder layer and substrate with a temperature of over 1253 K is transformed to the β phase. Due to the preheating from the first track, some powders in the second track are also transformed to the β phase. When the second track is scanned, the two-track powder layer and one part of the substrate becomes the β phase as given in (c). After a short cooling time at the end of the first layer, the martensite phase transformation occurs in the zone with a temperature lower than 923 K as given in (d). A repeated process of (a)-(d) on the second layer is given in (e)-(h). It is observed from (f) and (g) that a portion of the zone with the martensite α' phase is transformed to the β phase due to reheating from the second layer. After being cooled down to the ambient temperature, the printed part becomes martensite α' phase, as illustrated in (h).

Fig. 4.4. Schematic of SSPT in a multi-track and multi-layer SLM process: (a) a layer of powders is spread; (b) the first track is scanned; (c) the second track is scanned; (d) after cooling down at the end of first layer; (e)-(h): a repeated process of (a)-(d) on the second layer.

4.1.2. Mechanical model

In the mechanical model, temperature and solid-state phase fields obtained from the previous thermo-metallurgical model are used as input parameters to evaluate the thermal strain and volumetric change strain, respectively. The total strain increment $d\epsilon^{\text{Total}}$ is expressed by

$$d\epsilon^{\text{Total}} = d\epsilon^e + d\epsilon^p + d\epsilon^{\text{th}} + d\epsilon^{\text{tr}}, \quad (4.3)$$

where $d\epsilon^e$ is the elastic strain increment, $d\epsilon^p$ is the plastic strain increment, $d\epsilon^{\text{th}}$ is the thermal strain increment and $d\epsilon^{\text{tr}}$ is the volumetric change strain increment due to SSPT.

The volumetric change strain increment is given by [22]

$$d\epsilon^{\text{tr}} = \epsilon^{\Delta V}(T) \Delta f_i(T), \quad (4.4)$$

where $\epsilon^{\Delta V}$ is the volumetric change strain with a full SSPT and $f_i(T)$ is the phase volume fraction. The volumetric change strain $\epsilon^{\Delta V}$ can be determined by the lattice parameter of crystal structures for α and β phases [22, 193, 194]:

$$\epsilon^{\Delta V} = \frac{\sqrt[3]{V_\beta} - \sqrt[3]{V_\alpha}}{\sqrt[3]{V_\alpha}}, \quad (4.5)$$

where V_α and V_β are unit cell volumes of α and β phases, respectively. The unit cell volumes of α and β phases and the volume change strain $\epsilon^{\Delta V}$ are plotted in Fig. 4.5.

The volumetric change strain is obtained for the heating and cooling processes by substituting the volume fractions given in Eqs. (4.1) and (4.2) to Eq. (4.4):

$$d\epsilon^{\text{tr}} = \begin{cases} \alpha_h \Delta T, & \text{Heating} \\ \alpha_c \Delta T, & \text{Cooling} \end{cases}, \quad (4.6)$$

where

$$\begin{cases} \alpha_h = -0.00782 \exp[-0.0085(1253 - T)] \epsilon_h^{\Delta V}(T), & 473 \rightarrow 1253 \text{ K} \\ \alpha_c = -0.015 \exp[-0.015(923 - T)] \epsilon_c^{\Delta V}(T)_T, & 473 \leftarrow 923 \text{ K} \end{cases} \quad (4.7)$$

Since both the thermal strain and volumetric change strain are a function of the temperature gradient ΔT , they can be combined:

$$d\epsilon^{\text{th}} + d\epsilon^{\text{tr}} = \begin{cases} (\alpha_h + \alpha_T) \Delta T, & \text{Heating} \\ (\alpha_c + \alpha_T) \Delta T, & \text{Cooling} \end{cases} \quad (4.8)$$

Hence, SSPT is considered in the mechanical analysis by incorporating the volumetric change strain into the thermal strain in the FEM model.

Fig. 4.5. (a) Unit cell volumes for α and β phases and (b) volumetric change strain with a full SSPT [22].

4.1.3. Material properties

The thermophysical properties used in the coupling model include thermal conductivity, density and enthalpy for powders and bulk material. The density ρ_p is a function of the powder porosity as given in Eq. (3.11). The effective thermal conductivity k_p of the powder bed is estimated as [186]

$$\frac{k_p}{k_a} = (1 - \sqrt{1 - \phi}) \left(1 + \frac{\phi k_r}{k_a} \right) + \sqrt{1 - \phi} \left[\frac{2}{1 - k_a/k_b} \left(\frac{k_b}{k_a} \ln(k_b/k_a) - 1 \right) + \frac{k_r}{k_a} \right], \quad (4.9)$$

where k_a is the thermal conductivity of air, k_b is the thermal conductivity of the bulk material and k_r is the thermal conductivity portion due to radiation among powders:

$$k_r = 4F\delta T^3 d_r, \quad (4.10)$$

where δ is the Stefan-Boltzmann constant, d_r is the average diameter of powders and F is the view factor which is taken as 1/3. To take melting and vaporization processes into account, the latent heat is directly added to the enthalpy. The latent heat is taken as 286 kJ/kg for melting and 9830 kJ/kg for vaporization [22, 47, 189]. The temperature-dependent thermophysical properties of Ti6Al4V powders and bulk material are depicted in Fig. 4.6.

Fig. 4.6. Thermophysical properties of Ti6Al4V bulk and powders: (a) density [185], (b) heat conductivity [185] and (c) enthalpy [185, 195].

Fig. 4.7. A bilinear temperature-dependent constitutive relationship for Ti6Al4V [121, 130].

Fig. 4.8. Mechanical properties for Ti6Al4V: (a) the Young's modulus [130], (b) coefficient of thermal expansion [22], (c) Poisson ratio [130] and (d) yield stress [130].

A bilinear stress-strain constitutive relation is used for the plastic deformation as shown in Fig. 4.7, which requires the initial yield stress σ_{yield} and tangent modulus E_T as input parameters. The temperature-dependent mechanical properties of Ti6Al4V, including the Young's modulus E , Poisson ratio ν , the coefficient of thermal expansion (CTE) α_T , yield stress σ_{yield} and tangent modulus E_T , are plotted in Fig. 4.8.

4.2. Results and discussion

In this section, the temperature, solid-state phase and residual stress fields are analyzed, and the effect of SSPT on residual stresses is investigated based on the thermo-metallurgical-mechanical coupling model.

4.2.1. Temperature field

The thermal model is validated by comparing the width and depth of the simulated melt pool with experimental results. The liquid-solid phase distributions in the x - z cross section of the powder layer and substrate with various laser powers from 20 to 80 W are shown in Fig. 4.9. The solid and liquid phases are represented by blue and red colours, respectively. The simulation has been carried out with a scanning speed of 200 mm/s and a laser beam radius of 26 μm . The comparison between modelled melt pool sizes and experimental results [45] is illustrated in Fig. 4.10. From the comparison, the depth and width of the melt pool predicted by the model show good agreement with the experimental results.

Fig. 4.9. Liquid-solid phase fields in the x - z cross section of the melt track with different laser powers: (a) 20 W, (b) 40 W, (c) 60 W and (d) 80 W.

Fig. 4.10. Comparison between the simulation and experiment for melt pool sizes: (a) width and (b) depth.

The mesh of the FEM model and scanning pattern for a multi-layer SLM process are shown in Fig. 3.22. The material of both the substrate and powder layer is Ti6Al4V. A modelling scheme for the multi-layer SLM process considering the volumetric decrease due to the volume shrinkage of the powder bed is reported in our previous paper [12]. The modelling process parameters are listed in Table 4.3. The temperature evolution for a four-layer printing process is given in Fig. 4.11. After each layer is finished, the cooling time is

0.1 seconds. The scanned region by the laser beam becomes thinner which is induced by the volume shrinkage. Heat diffusion from the heated region to the surrounding fresh powders is slow because heat conductivity is low in the powder bed.

Table 4.3. Modelling process parameters for a multi-layer SLM process.

Parameter	Value
Layer thickness h (μm)	50
Laser beam radius r_0 (μm)	60
Laser power P (W)	100
Laser scanning speed v (mm/s)	600
Ambient temperature T_∞ (K)	298
Hatch distance D (μm)	72
Substrate $L \times W \times H$ (mm^3)	$1.5 \times 1.5 \times 0.5$
Powder layer $l \times w \times h$ (mm^3)	$0.5 \times 0.5 \times 0.05$

Fig. 4.11. Evolution of the temperature fields for a four-layer SLM process: (a) at the end of the first track, (b) at the end of the first layer, (c) at the middle time of the third layer and at the end of the fourth layer.

4.2.2. Solid-state phase field

The temperature and β phase fields in the x - z and y - z cross sections for a one-track printing process are demonstrated in Fig. 4.12. The powder bed consists of 92.5% α phase and 7.5% β phase at initial condition according to the Castro model in Eq. (4.1). The α phase starts to turn into the β phase with increasing temperature. As the temperature exceeds $B_f = 1253$ K, the α phase is fully transformed to the β phase. The material above around -40 μm in the z direction is completely transformed to the β phase around the scanned region by the laser beam. The volume fraction of the β phase gradually decreases in the region further away from the heat-affected zone.

Fig. 4.12. (a) temperature and (b) β phase fields in the x - z and y - z cross sections for a one-track printing process.

The evolution of the solid-state phase fields for a three-track printing process is shown in Fig. 4.13. In the first track, as shown in (a), the material with the temperature of over 1253 K is fully transformed to the β phase and the region with lower temperature is a combination of α and β phases. The martensite α' phase does not occur at this moment.

When the second track is being scanned, the first-track region mainly contains the β phase because the temperature in this region is still higher than the martensitic transformation temperature (923 K). Only a small region around the edge of the heat-affected zone in the substrate undergoes the martensitic transformation. When the third track is being scanned, the region with the β phase increases and more α' phase occurs at the bottom of the first-track heated region. After cooling down, most of the β phase in the heated zone is fully transformed to the α' phase and the region around the edge of the heat-affected zone is composed of α and α' phases.

Fig. 4.13. Solid-state phase fields for a three-track printing process in the x - z cross section of the melt track at the different time: (a) first track, (b) second track, (c) third track and (d) after cooling down.

The evolution of the temperature and phase volume fraction f_β at the central point of the first track on the top surface of the substrate for the three-track printing process is given in Fig. 4.14. Three peak points occur in the temperature history because the point is heated three times while three tracks are scanned by the laser beam sequentially. The peak temperature decreases because the laser beam is gradually moving away from the point. When the laser beam reaches the point, the temperature dramatically increases to 2480 K and the α phase completely becomes the β phase at the same time. Then, the point remains in the β phase until the three tracks are scanned. After the scanning is finished, the temperature at the point gradually decreases to the martensitic transformation temperature M_s . The β phase can be fully transformed to the martensite α' phase due to the fast cooling rate.

Fig. 4.14. Evolution of the (a) temperature and (b) phase volume fraction f_β for a three-track printing process.

4.2.3. Residual stress field

In this case, the stress fields for a one-layer printing process are first investigated. The unidirectional scanning strategy with a hatch distance $D = 1.2r_0$ is used. The printing process parameters including laser parameters and the powder layer thickness are the same as those listed in Table 4.3 for the multi-layer printing process. In addition, the substrate plate is fixed for the mechanical analysis:

$$\begin{cases} w = 0, & \text{at } z = -H \\ u = 0, & \text{at } x=0 \text{ and } W, \\ v = 0, & \text{at } y=0 \text{ and } L \end{cases} \quad (4.11)$$

where u , v and w are the displacements in the x , y and z directions, respectively; W , L and H are the length, width and height of the substrate plate, respectively.

Fig. 4.15 illustrates the comparison of von Mises stress fields without and with SSPT for the one-layer printing process at different instances of time. The printing time t_m of the one-layer eight-track printing process is 6.67×10^{-3} s. The printed product approaches the ambient temperature $T_\infty = 298$ K after 500 seconds of cooling. Fig. 4.15 (a) gives the von Mises stress fields when one half of the powder bed is scanned by the laser beam. The grey part represents deactivated fresh powders which have no influence on the mechanical analysis of the printed part. The blue region has low stress because this region is the melt pool where the Young's modulus and yield stress are assumed to be very small. Large stresses occur at the first-track region at the end of the laser scanning process as shown in Fig. 4.15 (b), which is caused by the formation of tensile residual stresses after the laser beam moves away. The von Mises stress increases as the temperature declines during the cooling process, as given in Figs. 4.15 (c) and (d). The large stress is distributed along the

scanning track because these regions suffer from high-temperature gradients, which is consistent with the reported results in [135, 196].

Fig. 4.15. Evolution of von Mises stress fields with and without SSPT for the one-layer printing process: (a) $t_m/2$, (b) t_m , (c) t_m+1 and (d) t_m+500 .

Fig. 4.15 also shows that the stress level with SSPT is smaller than that without SSPT during laser scanning and cooling processes. This can be explained by the volumetric change schematic due to SSPT as shown in Fig. 4.2. The material volume decreases during the heating process and increases during the cooling process, which is different from the thermal expansion and contraction caused by the temperature change. As a result, SSPT and thermal gradients have an opposite effect on the evolution of the thermal stress field.

Fig. 4.16. Residual stress fields with and without SSPT for the one-layer printed product: (a) σ_x , (b) σ_y and (c) σ_z .

Residual stress components with and without SSPT for the one-layer printed product are demonstrated in Fig. 4.16. Compared with the other two stress components σ_x and σ_z , the stress component σ_y has the maximum stress because the scanning direction is in the y direction such that the newly printed part is constrained by previously printed parts. Hence, we can see that a large stress value occurs along the scanning track. Furthermore, the z -direction stress component is smaller than the other two stress components because of the free constraint on the top surface. For stress components σ_x , σ_y and σ_z , the tensile stress level with SSPT is larger than those without SSPT, which is consistent with the comparison of von Mises stress fields in Fig. 4.16. In addition, larger compressive stresses are observed when SSPT is considered.

Fig. 4.17. Residual stress fields for a four-layer printed product with SSPT: (a) σ_y and (b) σ_{von} .

The significance of SSPT has been proved from the stress fields in the one-layer printing process. In this case, residual stress fields for a four-layer printed product with consideration of SSPT are presented. The FEM model and printing process parameters are given in Fig. 4.11 and Table 4.3, respectively. The residual stress component σ_y and von Mises stress σ_{von} for a four-layer printed product are shown in Fig. 4.17. For the stress component σ_y , the maximum tensile and compressive stresses occur at the bottom and top surfaces, respectively. To observe the stress fields in the thickness direction, the residual stress field in the x - z cross section at the center of the four-layer printed product is given. The stress components σ_y has tensile stress in the first three layers and compressive stress in the fourth-layer region. Fig. 4.17 (b) shows that the maximum von Mises stress is at the bottom just like the stress component σ_y and the minimum stress is at the middle region, which is also observed in other reported simulation results [132, 133]. In addition, the maximum stress in the four-layer printed product (871 MPa) is larger than the maximum von Mises stress of the one-layer product (606 MPa).

4.3. Summary

Despite great advances in the thermo-mechanical modelling of the SLM additive manufacturing process, a comprehensive understanding of thermal and mechanical behaviours with the consideration of SSPT phenomena is rarely reported. Hence, this chapter has investigated the thermo-metallurgical-mechanical behaviours of the Ti6Al4V product manufactured by SLM technique. The developed model which consists of thermal, metallurgical and mechanical analyses can predict the temperature, solid-phase and stress fields during the SLM additive manufacturing process. The effect of the repeated SSPT on

residual stress fields in the multi-track and multi-layer process has been studied. The modelling results show that consideration of SSPT gives rise to the decrease of tensile residual stresses and the increase of compressive residual stresses.

Chapter 5. Modelling Thermo-Fluid Behaviours for Selective Laser Melting Additive Manufacturing of Stainless Steel 316L

Porosity, one of the main defects in the SLM-built products, are greatly influenced by the melt pool dynamics in the SLM process. In the SLM additive manufacturing process, there exist complex multi-physics phenomena which are influenced by many processing parameters and environmental factors. To alleviate the porosity of printed products, it is essential to investigate the effect of these factors on the pore formation and analyze the underlying mechanism. In this Chapter, a powder-scale CFD model has been developed to investigate the keyhole dynamics and porosity for SLM of stainless steel 316L. In this model, the main driven forces of melt flow, including the surface tension, damping force in the liquid-solid mushy zone, Marangoni force and recoil pressure, are considered. Furthermore, the effects of laser power, scanning speed and ambient pressures on the melt flow dynamics and porosity in the SLM process are analyzed.

5.1. Development of a powder-scale multi-physics model

DEM is applied to simulate the packing process of the powder bed for SLM. For DEM modelling, the translational and rotational motion of powders are dependent on Newton's second law and the Hertz-Mindlin contact model is used [97]. The cohesive forces such as Van der Waals force and electrostatic force are neglected because the magnitude of these two forces is much smaller than the contact force for the size scale of metal powders used in this model [197]. In the powder packing process, a certain amount of powders subjected to gravity and contact forces is randomly dropped down to a chamber. Subsequently, the powder bed with a height of 50 μm is formed as a recoating blade removes extra powders.

The size of powders follows the normal distribution with a mean radius of 27 μm and a full width at half maximum of 1.18. The parameters used in the packing process simulation of the stainless steel 316L powders are listed in Table 5.1.

Table 5.1. Processing parameters used for the DEM simulation of stainless steel 316L [197, 198].

Parameter	Value
Density ρ (kg/m^3)	7800
Young's modulus E (GPa)	193
Poisson ratio ν	0.265
Coefficient of Restitution e	0.5
Friction efficient μ_f	0.577
Acceleration of gravity g (m/s^2)	9.8

SLM is a complex multi-scale and multi-physics process which consists of heat conduction, convection, radiation and mass transfer. To simulate the heat flux and mass transport, a thermo-fluid model needs to be developed based on the mass continuity, momentum conservation and energy conservation equations. In this work, the developed model is solved by using the ANSYS Fluent [199], which is a commercial CFD software built on the finite volume method. The CFD model considers important driving forces such as surface tension, thermo-capillary force, damping force in the mushy zone of liquid/solid transition and recoil force due to metal vaporization. Additionally, the heat and mass transfer in the phase transformation process of melting, solidification and vaporization is taken into account. To simplify the model, the following assumptions are made: firstly, the

fluid flow in the melt pool is assumed to be a laminar flow of Newtonian fluid; secondly, metal vapor flow is ignored and the interaction between vapor and powders is not considered; thirdly, the Fresnel reflection of the laser beam is neglected.

5.1.1 Volume of fluid method

The volume of fluid (VOF) method is used to track the liquid/air interface. For the VOF method, the material properties of the mixture phase are evaluated by a weight function [28]:

$$\varphi = \varphi_{\text{metal}} \alpha_1 + \varphi_{\text{air}} \alpha_2, \quad (5.1)$$

where φ_{metal} and φ_{air} are the material properties of metal and air phases, respectively; α_1 and α_2 are the volume fractions of metal and air phases, respectively, which satisfy the following relationship:

$$\alpha_1 + \alpha_2 = 1. \quad (5.2)$$

The tracking of the liquid/air interface is accomplished by solving the continuity equation for phase volume fractions. The volume fraction of the metal phase has the following conservation equation [28]:

$$\frac{\partial}{\partial t} (\alpha_1 \rho) + \nabla \cdot (\alpha_1 \rho \vec{u}) = 0, \quad (5.3)$$

where ρ is the density of the mixture phase and $\vec{u}$ is the velocity vector.

5.1.2. Thermo-fluid dynamics model

The transport phenomena for SLM is described based on the mass, momentum and energy conservation equations. Furthermore, the effect of the ambient pressure on the recoil pressure and vaporization temperature is incorporated into the CFD model. The mass

continuity equation is expressed as [200]

$$\frac{\partial \rho}{\partial t} + \nabla \cdot (\rho \vec{u}) = 0. \quad (5.4)$$

The momentum equation is written as [199]

$$\frac{\partial(\rho \vec{u})}{\partial t} + \nabla \cdot (\rho \vec{u} \otimes \vec{u}) = -\nabla P + \rho \vec{G} + \nabla \cdot (\mu \nabla \vec{u}) + \vec{S}_f, \quad (5.5)$$

where P is the pressure, $\vec{G}$ is the acceleration of gravity, μ is the dynamic viscosity and $\vec{S}_f$ is the momentum source vector which is written by

$$\vec{S}_f = \vec{f}_d + \vec{f}_s + \vec{f}_r, \quad (5.6)$$

where $\vec{f}_d$ is the momentum sink term which is used to consider pressure drop due to the solidification process of the molten metal in the liquid-solid mushy zone, $\vec{f}_s$ is the surface tension force and $\vec{f}_r$ is the recoil pressure due to metal vaporization.

The momentum sink term $\vec{f}_d$ in the liquid-solid mushy zone is defined as [199]

$$\vec{f}_d = A_m \frac{(1-\beta)^2}{(\beta^3 + \varepsilon)} (\vec{u} - \vec{u}_p), \quad (5.7)$$

where β is the liquid volume fraction in the mushy zone, ε is a small constant (0.001) introduced to avoid leading to a denominator of zero, A_m is the mushy zone constant and $\vec{u}_p$ is the pull velocity of the solid which is taken as zero. In the mushy zone, the liquid fraction β is defined as

$$\beta = \begin{cases} 0 & T < T_s \\ \frac{T - T_s}{T_L - T_s} & T_s \leq T \leq T_L, \\ 1 & T_L < T \end{cases} \quad (5.8)$$

where T_s and T_L are solidus and liquidus temperature, respectively. The material properties

such as thermal conductivity and density in the mushy zone are estimated as

$$\varphi = \beta \varphi_L + (1 - \beta) \varphi_S, \quad (5.9)$$

where φ_L and φ_S are the material properties of molten and solid metals, respectively.

The continuum surface force model has been employed to incorporate the surface tension into the momentum equation as a source term. In Fluent software, the surface tension force is expressed as a volume force using the divergence theorem. The surface tension source has the following form [201]:

$$\vec{f}_s = \sigma \kappa \vec{n} |\nabla \alpha_1| \frac{2\rho}{\rho_{\text{metal}} + \rho_{\text{air}}}, \quad (5.10)$$

where σ is the surface tension coefficient of the molten metal, $\vec{n}$ is the normal unit vector at the metal/air interface and κ is the curvature of the metal/air interface. Moreover, $|\nabla \alpha_1|$ is used to transform the interfacial force to a volumetric force.

The recoil pressure $\vec{f}_r$ which considers the surface pressure due to metal vaporization and ambient pressure is expressed as [160, 202]

$$\vec{f}_r = (P_s - P_{\text{amb}}) \vec{n} |\nabla \alpha_1| \frac{2\rho}{\rho_{\text{metal}} + \rho_{\text{air}}}, \quad (5.11)$$

where P_{amb} and P_s are the ambient pressure and surface pressure, respectively. The surface pressure P_s is defined as [160]

$$P_s = \begin{cases} P_{\text{amb}} & T < T_v \\ 0.54 P_{\text{amb}} \exp\left(\frac{L_v M (T - T_v)}{R T T_v}\right) & T \geq T_v \end{cases}, \quad (5.12)$$

where L_v is the latent heat of vaporization, T_v is the vaporization temperature, M is the molar mass and R is the universal gas constant. A relationship between vaporization temperature and ambient pressure for stainless steel 316L can be estimated by the following

empirical form [203]:

$$\log(P_{\text{amb}}) = 11.1183 - \frac{18868}{T_v}. \quad (5.13)$$

The vaporization temperature via the ambient temperature is plotted in Fig. 5.1.

Fig. 5.1. Vaporization temperature via the ambient pressure for stainless steel 316L [203].

The energy conservation equation is expressed as [199]

$$\frac{\partial(\rho H)}{\partial t} + \nabla \cdot (\rho \vec{u} H) = \nabla \cdot (k \nabla T) + S_E, \quad (5.14)$$

where k is the thermal conductivity and H is the enthalpy:

$$H = H_{\text{ref}} + \int_{T_{\text{ref}}}^T C_p dT + \beta L_m, \quad (5.15)$$

where H_{ref} is the reference enthalpy, T_{ref} is the reference temperature, C_p is the specific heat capacity and L_m is the latent heat of melting.

The energy source term S_E is given as

$$S_E = q_v + q_{\text{rad}} + q_L, \quad (5.16)$$

where q_v is the heat loss due to metal vaporization, q_{rad} is the radiation heat to the ambient environment and q_L is the laser heat source. The heat loss due to metal vaporization is expressed as [204]

$$q_v = -0.82 \frac{L_v M}{\sqrt{2\pi MRT}} P_{\text{amb}} \exp\left(\frac{L_v M (T - T_v)}{RTT_v}\right) |\nabla \alpha_1| \frac{2\overline{\rho C_p}}{\rho_{\text{metal}} C_{\text{metal}} + \rho_{\text{air}} C_{\text{air}}}. \quad (5.17)$$

The heat loss due to the radiation is given as [24]

$$q_{\text{rad}} = -\sigma_s \varepsilon (T^4 - T_\infty^4) |\nabla \alpha_1| \frac{2\overline{\rho C_p}}{\rho_{\text{metal}} C_{\text{metal}} + \rho_{\text{air}} C_{\text{air}}}, \quad (5.18)$$

where σ_s is the Stephan-Boltzmann constant, ε is the emissivity and T_∞ is the ambient temperature.

To represent the laser heat flux, a simplified ray-trace heat source is used [26, 31]. In this method, to ensure the laser beam is exerted on the top surface of the metal phase, an index f_a is employed for capturing air/metal interface. The value of f_a ranges from 0 to 1, which is determined by the volume fraction of the metal phase. More details to evaluate f_a can be found in [31]. Hence, the heat source used in this model is expressed as

$$q_L = \frac{f_a Q}{\Delta y}, \quad (5.19)$$

where f_a which ranges from 0 to 1 is used for indicating the top surface of gas/metal interface, Δy is the mesh size in the irradiation direction of the laser beam (y direction in this model) and Q is the Gaussian distribution which is expressed as [205, 206]

$$Q = \frac{2AP}{\pi r_0^2} \exp\left[-2 \frac{(x - x_0 + vt)^2 + (z - z_0)^2}{r_0^2}\right], \quad (5.20)$$

where A is the absorptivity of the laser beam, P is the laser power, r_0 is the laser beam radius, v is the laser scanning speed and (x_0, z_0) is the starting point of laser scanning.

5.2. Results and discussion

A 3D computational domain with a size of $L \times W \times H = 500 \times 200 \times 250 \mu\text{m}^3$ is used to model a single-track laser melting process of stainless steel 316L, as given in Fig. 5.2. The computational domain is divided with a mesh size of $3 \times 3 \times 3 \mu\text{m}^3$. A layer of powders with a height of $50 \mu\text{m}$ is spread on the substrate of stainless steel 316L. The packing process of the powder bed is simulated using DEM. The lateral and bottom surfaces of the computational domain are assumed to be insulated. Furthermore, the upper surface of the air domain is approximated as the pressure outlet boundary condition. The thermophysical properties of stainless steel 316L and processing parameters used in this model are given in Table 5.2.

Fig. 5.2. Computational domain for simulating a single-track SLM process.

Table 5.2. Thermophysical properties of stainless steel 316L and processing parameters for simulating the SLM process [26, 207].

Parameter	Value
Solidus temperature T_s (K)	1658
Liquidus temperature T_L (K)	1723
Specific heat C (J/(K·kg))	710
Thermal conductivity k (W/(m·K))	$9.248 + 0.01571T, T \leq T_s$ $12.41 + 0.003279T, T > T_L$
Density ρ (kg/m ³)	$8084 - 0.4209T - 3.894 \times 10^{-5}T^2, T \leq T_s$ $7433 - 0.393T - 1.8 \times 10^{-4}T^2, T > T_L$
Vaporization temperature T_v (K)	3090
Latent heat of melting L_m (J/kg)	2.7×10^5
Latent heat of vaporization L_v (J/kg)	7.5×10^6
Viscosity of molten metal μ (m ² /s)	0.005
Surface tension σ (N/m)	$1.77 - 4.3 \times 10^{-4} \times (T - 1723)$
Mushy zone constant A_m	1×10^8
Molar mass M (kg/mol)	0.05593
Universal gas constant R (J/mol)	8.314
Stefan-Boltzmann constant σ_s (W·m/K ⁴)	5.67×10^{-8}
Emissivity ε	0.26
Laser absorptivity A	0.42
Laser beam radius r_0 (μm)	27
Ambient temperature T_∞ (K)	300

5.2.1. Model validation

The comparison between modelling and experiment for the melt pool size is given in Table 5.3. The transverse cross section (x - z plane) of melt track with various laser powers and scanning speeds is demonstrated in Fig 5.3. The simulated depth and width of the melt track show a good agreement with the experimental results. The laser absorptivity $A = 0.42$ is used under the atmospheric pressure. The vapor attenuation of the laser beam under different ambient pressures is shown in Fig. 5.4. About 13 % of laser irradiation is attenuated by the metal vapor at the atmospheric pressure. The laser absorptivity under the sub-atmospheric pressure is approximated by the following formula:

$$A = 0.42 + (13\% - A_{\text{vapor}}), \quad (5.21)$$

where A_{vapor} is the vapor attenuation of the laser beam under the sub-atmospheric pressure. The vapor attenuation A_{vapor} at ambient pressures $P_{\text{amb}} = 10^3$ and 10 Pa is 3% and 2%, respectively. Hence, the laser absorptivity at these two sub-atmospheric pressures is found out to be 0.52 and 0.53, respectively.

Fig. 5.3. The x - z cross section of the melt track: (a) $P = 300$ W and $v = 1500$ mm/s, (b) $P = 200$ W and $v = 1200$ mm/s and (c) $P = 200$ W and $v = 1500$ mm/s.

Table 5.3. Comparison between modelling and experiment for the melt pool size.

P (W)	v (mm/s)	Depth (μm)		Width (μm)	
		Experiment [208]	Simulation	Experiment [208]	Simulation
300	1500	94	92.3	83	76.0
200	1200	69	64.7	104	79.1
200	1500	57	52.5	84	74.5

Fig. 5.4. Vapor attenuation of the laser beam via the ambient pressure [209].

5.2.2. Laser power effect

The evolution of the melt track and keyhole is demonstrated in Fig. 5.5. The melt pool is confined within the black line ($T > 1658$ K). As the laser is applied on the powder bed, the powders irradiated by the laser beam are melted and a keyhole is formed as shown in the x - y cross section of Fig. 5.5 (a). Keyhole formation results from the balance of the recoil pressure due to metal vapor, ambient pressure, surface tension and hydrostatic pressure.

The recoil pressure tends to overcome the ambient pressure, surface tension and hydrostatic pressure to keep the keyhole open. Under the effect of the recoil pressure, the molten metal is pushed outwards from the keyhole. As more energy is absorbed by powders, the keyhole becomes deeper. The laser radiation is directly deposited to the substrate when the keyhole penetrates through the powder bed. Figs. 5.5 (b) and (c) illustrate that a continuous melt track is formed with the movement of the laser and the penetration depth of the keyhole becomes stable.

Fig. 5.5. Evolution of the melt track and keyhole with $P = 200$ W and $v = 1500$ mm/s at different time: (a) $t = 10$ μ s, (b) $t = 100$ μ s and (c) $t = 200$ μ s.

The temperature and melt velocity fields at $P = 100$, 200 and 300 W are illustrated in Fig. 5.6. From the top view of the temperature fields, the length of the melt pool increases as the laser power is elevated. The outward velocity is observed around the keyhole edge

because the molten metals are pushed outwards from the keyhole under the effect of the recoil pressure. Behind the keyhole, the molten metals tend to flow to the surroundings due to the Marangoni force. The Marangoni force due to the gradient of the surface tension drives the fluid to flow from the high-temperature region to the low-temperature region. Whereas, velocity on the edge of the melt track is directed to the centerline of the melt track because the surface tension tends to minimize the surface area.

Fig. 5.6. Temperature and melt flow fields with different laser powers ($v = 1500$ m/s): (a) $P = 100$ W, (b) $P = 200$ W and (c) $P = 300$ W.

From the temperature field of the x - y cross section at $z = 100$ μm , the melt pool depth increases with the increasing of the laser power. A shallow keyhole is observed at $P = 100$ W and the melt pool depth approximates to the thickness of the powder layer. At $P = 200$

W, the keyhole penetrates through the powder bed into the substrate. The downward and upward velocities are observed on the front and rear surfaces of the keyhole, respectively. The recoil pressure due to the intensive metal vaporization drives the molten metal flow downwards on the front surface of the keyhole. Afterwards, the downward molten metals from the front surface encounter the solid metal on the bottom of the keyhole and are pushed backwards. Hence, large velocities are observed at the rear surface of the keyhole and at the bottom of the melt pool in the rear melt pool. As the laser power is increased to 300 W, the rear surface of the keyhole becomes unstable. Hence, a hump is formed on the rear surface of the keyhole due to the interaction of the upward molten metal from the front surface and the downward melt driven by the vortices in the rear part of the melt pool. The hump is directly irradiated by the laser beam and heated to a high temperature. As the temperature is over the vaporization temperature, the metal is vaporized, which causes a large recoil pressure on the hump. Hence, a large downward velocity can be observed on the hump, which may result in the collapse of the rear surface and then trap the bubble in the melt pool.

The temperature fields of the x - y cross sections at $z = 110 \mu\text{m}$ and $120 \mu\text{m}$ with various laser powers $P = 100, 200$ and 300 W are shown in Fig. 5.7. Irregular pores are formed at the boundary of the powder bed and substrate at $P = 100 \text{ W}$ due to the insufficient laser energy density. No pore occurs in the cross section at the center of the laser beam, as shown in Fig. 5.7 (a) because the laser energy is enough to melt the substrate. However, more pores are present near the beam boundary ($z = 120 \mu\text{m}$). The presence of the irregular pores is attributed to suppressed wetting behaviours between the melted powders and the substrate [26]. As the laser energy density is not enough to fuse the substrate, the melted

powders tend to bond each other rather than the un-melted substrate. In addition, the pores are observed near to the bottom of the keyhole at $P = 300$ W. Such pores are induced by the keyhole instability due to the high laser energy density. The formation of keyhole-induced pores is demonstrated in Fig. 5.8 and the mechanism is explained in the following paragraph.

Fig. 5.7. Temperature fields with different laser powers ($v = 1500$ m/s): (a) $P = 100$ W, (b) $P = 200$ W and (c) $P = 300$ W.

The formation process of keyhole-induced pores is summarized as follows: initially, the hump occurs on the rear surface of the keyhole; Afterwards, the keyhole wall with the hump collapse to trap bubbles in the melt pool; Finally, the trapped bubbles are captured on the liquid/solid interface to develop pores, as shown in Fig. 5.8. A hump is formed at

the rear surface of the keyhole in Fig. 5.8 (a). The hump formation results from the combined effect of the upward melt flow due to the recoil pressure and downward vortices on the rear surface of the keyhole. The upper surface of the hump is directly irradiated by the laser beam, which results in intensive metal vaporization. Afterwards, the metal vapor would apply a high recoil pressure on the hump. Under the influence of the downward recoil pressure and clock-wise vortices flow, the rear wall of the keyhole may collapse. The collapse may trap air bubbles inside the molten metal and thus lead to the pore formation as depicted in Fig. 5.8 (b). The bubble is driven backwards by the strong vortex flow until the bubble is captured by the solidification front as shown in Fig. 5.8 (c) and eventually a pore is formed as shown in Fig. 5.8 (d).

Fig. 5.8. Formation process of keyhole-induced pores ($P = 300 \text{ W}$ and $v = 1500 \text{ mm/s}$) at different time: (a) $t = 100 \mu\text{s}$, (b) $t = 110 \mu\text{s}$, (c) $t = 122 \mu\text{s}$ and (d) $t = 125 \mu\text{s}$.

5.2.3. Scanning speed effect

The effect of the laser scanning speed on the melt flow behaviours and porosity is investigated in this case. The temperature and melt flow velocity fields at various scanning speeds $v = 800$ mm/s, 1500 mm/s and 4000 mm/s are shown in Fig. 5.9. it can be observed that the length of the melt pool increases with the rising of the laser scanning speed. This can be explained that lower scanning speed has more time to diffuse the heat when the laser scans the same distance.

Fig. 5.9. Temperature and melt flow velocity fields with different scanning speeds ($P = 200$ W): (a) $v = 800$ mm/s, (b) $v = 1500$ mm/s and (c) $v = 4000$ mm/s.

From the top view of the melt track, the width of the melt track increases with the reduction of the laser scanning speed because metals have more time to absorb the laser

energy within the same distance. At $v = 800$ mm/s, a pore is observed at the bottom of the keyhole, which is similar to the pore formation at high laser power. At the high laser scanning speed ($v = 4000$ mm/s), the irregular pores due to the insufficient laser energy density are observed. Two large pores on the top surface of the melt track are formed behind the keyhole as shown in Fig. 5.9 (c), which leads to the discontinuous track.

Fig. 5.10. Temperature fields with different laser scanning speeds ($P = 200$ W): (a) $v = 800$ mm/s, (b) $v = 1500$ mm/s and (c) $v = 4000$ mm/s.

The temperature fields of the x - y cross sections at $z = 110$ and 120 μm with various laser scanning speeds $v = 800$, 1500 and 4000 mm/s are given in Fig. 5.10. The pores are formed at low scanning speed ($v = 800$ mm/s) and high scanning speed ($v = 4000$ mm/s),

which are ascribed to the keyhole instability and lack of fusion, respectively. Low scanning speeds result in the keyhole-induced pores, which is similar to the high laser energy. Additionally, the high scanning speed like the low laser power causes the irregular pores at the interface of the powder bed and substrate.

5.2.4. Ambient pressure effect

The laser melting of metals under the sub-atmospheric pressure has been paid more attentions due to its advantages such as the improved penetration depth, reduced porosity and sound melt track [56, 57]. In this case, the effect of the ambient pressure on the surface temperature and surface pressure will be analyzed.

Fig. 5.11. 3D clips for temperature and recoil pressure fields under different ambient pressures at $t = 200 \mu\text{s}$ ($P = 200 \text{ W}$ and $v = 1500 \text{ mm/s}$): (a) $P_{\text{amb}} = 10 \text{ Pa}$, (b) $P_{\text{amb}} = 10^3$ and (c) $P_{\text{amb}} = 10^5 \text{ Pa}$.

The 3D clips for temperature and surface pressure fields under different ambient pressures are shown in Fig. 5.11. The high temperature and surface pressure are mainly distributed on the front surface of the keyhole because the center of the deposited laser beam is located at this position. The variation of the average temperature and pressure on the keyhole surface with different ambient pressures are given in Fig 5.12. The higher average temperature on the keyhole surface is observed at higher ambient pressures because of the increase of the vaporization temperature. Furthermore, the surface pressure reduces as the ambient pressure increases.

Fig. 5.12. Average surface temperature and recoil pressure of the keyhole via the ambient pressure.

The variation of melt pool depth with different ambient pressures is presented in Fig. 5.13. The depth of the melt pool decreases with increasing the ambient pressure. Nevertheless, the variation of the penetration depth with the reduction in the ambient pressure is not significant under low ambient pressures. The reduced effect of the ambient pressure on the penetration depth is attributed to two main reasons. Firstly, as shown in Fig.

5.4, the effect of the ambient pressure on the vapor attenuation of the laser radiation reduces under low ambient pressures. As the ambient pressure is below $P_{\text{amb}} = 10^3$ Pa, the vapor attenuation of laser radiation is almost unchanged and keeps at about 2.5 %. Secondly, there exists a critical ambient pressure which influences the force balance to create and maintain the keyhole.

Fig. 5.13. Melt pool depth via the ambient pressure.

A schematic for the balance forces to maintain the keyhole open is illustrated in Fig. 5.14. A required surface pressure P_s which exists inside the keyhole to keep the keyhole open is given as [57]

$$P_s = P_{\text{amb}} + P_c + P_h + P_v(v), \quad (5.22)$$

where P_{amb} is the ambient pressure, P_c is the surface tension, P_h is the hydrostatic pressure and $P_v(v)$ is a function that considers the displacement of the keyhole with a scanning speed v . By neglecting the hydrostatic pressure P_h and $P_v(v)$, the force balance Eq. (5.22) can be approximated as

$$P_s \approx P_{\text{amb}} + P_c. \quad (5.23)$$

The critical ambient pressure which has a non-negligible effect on the keyhole is given as [57]

$$P_{\text{CR}} = P_c / 10, \quad (5.24)$$

where the surface tension force $P_c = \sigma/r_k$, in which the radius of keyhole r_k is assumed to be equal to the laser beam radius $r_0 = 27 \mu\text{m}$. Hence, the critical ambient pressure P_{CR} is estimated as $6.5 \times 10^3 \text{ Pa}$ by using the surface tension in Table 5.2 at liquidus temperature $T_L = 1723 \text{ K}$.

Fig. 5.14. Schematic of balance forces to form and maintain the keyhole in the melt pool.

The temperature and velocity fields in the x - y and y - z cross sections of the melt track under different ambient pressures are shown in Fig. 5.15. The transverse y - z cross section is located at the center of the laser beam. A downward velocity is found on the front wall of the keyhole in the x - y cross section and the keyhole surfaces in the y - z cross section because of the effect of the recoil pressure. As shown in Fig. 5.15, the high surface pressure is exerted on the front surface of the keyhole because the metal directly irradiated by the high-intensity laser beam is vaporized to develop large recoil pressures. Under the effect of the recoil pressure, the molten metal at the bottom of the rear melt pool is pushed

backwards along the solidification front and upwards along the rear surface of the keyhole.

Fig. 5.15. Temperature and velocity fields in the x - y and z - y cross sections of the melt pool under different ambient pressures ($P = 200$ W and $v = 1500$ mm/s): (a) $P_{\text{amb}} = 10$ Pa, (b) $P_{\text{amb}} = 10^3$ Pa and (c) $P_{\text{amb}} = 10^5$ Pa.

From Fig. 5.15, we also can see that the surface tension plays a more important role on the rear surface of the keyhole because the metal vaporization on the rear surface of the keyhole is not as intensive as that on the front keyhole surface. On the upper surface of the rear melt pool, a backward velocity is observed. Such a backward flow trend is mainly caused by the Marangoni effect due to the surface tension gradient. A clock-wise vortex is

also observed inside the rear part of the melt pool which may lead to the hump formation and thus give rise to the collapse of the rear surface of the keyhole. The velocity magnitude on the melt pool surface under sub-atmospheric pressures $P_{\text{amb}} = 10$ and 10^3 Pa is larger than that under the atmospheric pressure as there exist higher recoil pressures under the sub-atmospheric pressure.

The 3D contours of the pore distribution under ambient pressures $P_{\text{amb}} = 10$, 10^3 and 10^5 Pa are illustrated in Fig. 5.16. As compared with the pore distribution under the atmospheric pressure, fewer pores are formed under the sub-atmospheric pressure. On the other hand, there exists a deeper keyhole depth under the sub-atmospheric pressure, which may increase the keyhole instability and then cause the keyhole collapse. However, the width of keyhole under the sub-atmospheric pressure is larger than that at the atmospheric pressure. The wider keyhole under the sub-atmospheric pressure is attributed to the higher average recoil pressure (See Fig. 5.12) and more vaporized metals due to the low vaporization temperature. A wider keyhole can reduce the possibility of pore formation by preventing the contact between the rear and front walls of the keyhole. Moreover, a higher recoil pressure is in favor of maintaining the keyhole. Meanwhile, the recoil pressure can occur at a lower vaporization temperature and cover more keyhole surface area under the sub-atmospheric pressure, especially for the rear keyhole surface with a lower temperature. Additionally, a faster upward metal flow due to the impact force of metal vapor ejected from the bottom of the keyhole is favorable for the bubble to escape from the melt pool. Finally, a low mean surface temperature due to a lower vaporization temperature, as given in Fig. 5.12, can decrease the temperature gradient in the keyhole and thus reduce the instability of the keyhole surface.

Fig. 5.16. 3D contours for the pore distribution under different ambient pressures ($P = 300$ W and $v = 1500$ mm/s): (a) $P_{\text{amb}} = 10$ Pa, (b) $P_{\text{amb}} = 10^3$ Pa and (c) $P_{\text{amb}} = 10^5$ Pa.

5.3. Summary

A powder-scale multi-physics model has been developed to investigate the thermo-fluid behaviors and pore formation in stainless steel 316L during the SLM additive manufacturing process. The effects of the laser power, scanning speed and ambient pressure on the thermo-fluid dynamics and pore formation in the melt pool have been analyzed. The simulation results show that the excessive laser energy density due to the high laser power or low scanning speed may result in the formation of keyhole-induced pores. Additionally, the inadequate laser energy density due to the low laser power or high scanning speed may lead to the irregular pores at the interlayer.

Due to the reduced vaporization temperature and vapor attenuation of laser irradiation, the melt pool depth increases with the reduced ambient pressure. Whereas, the effect of the ambient pressure on the penetration depth is not noticeable under a critical ambient pressure. As compared with the atmospheric pressure, the sub-atmospheric pressure can lead to lower average temperature and larger average recoil pressure on the keyhole surface, which is beneficial to the keyhole stability and thus minimize the pore defects of SLM-built products.

Chapter 6. Conclusions and Recommendations

This chapter draws the main conclusions of this Ph.D. research and then presents the recommendation to the future work.

6.1. Conclusions

In this Ph.D. research, the process modelling of SLM additive manufacturing has been conducted for investigating the thermal, mechanical and fluid-dynamic behaviours of printed products. Firstly, a thermo-mechanical coupling model has been developed to simulate the multi-track and multi-layer SLM process of stainless steel 316L. The model considers temperature-dependent material properties and the powder-liquid-solid phase transition which includes melting, vaporization, solidification, shrinkage and cooling phenomena. Furthermore, a thermo-metallurgical-mechanical coupling model has been developed to investigate the effect of SSPT on the residual stresses of Ti6Al4V. Finally, a powder-scale multi-physics model has been developed to explore the thermo-fluid behaviours in the stainless steel 316L during the SLM additive manufacturing process.

Based on the thermo-mechanical model, thermal analysis reveals that both increasing laser power and decreasing scanning speed can contribute to the increase of the melt pool size. The modelling temperature field is symmetric on the plane perpendicular to the scanning direction. There exists a dramatic temperature gradient between molten metals and surrounding powders. The mechanical analysis suggests that the stress component along the scanning direction is larger than the other two stress components. Large residual stresses are distributed in the first two layers of the multi-layer printed product due to the constraints of the substrate.

Despite great advances in the thermo-mechanical modelling of the SLM additive manufacturing process, a comprehensive understanding of thermal and mechanical behaviours with the consideration of SSPT is rarely reported. Hence, the thermo-metallurgical-mechanical behaviors of Ti6Al4V products have been investigated during the SLM additive manufacturing process. The effect of SSPT on residual stress fields in the multi-track products has been studied. The modelling results show that the consideration of SSPT can give rise to the decrease of tensile residual stresses and the increase of compressive residual stresses.

Simulation results obtained from the thermo-fluid model show that the excessive laser intensity due to the high laser power or low scanning speed may result in the formation of keyhole-induced pores. Additionally, the insufficient laser intensity due to the low laser power or high scanning speed may lead to the irregular pores at the interlayer. Due to the reduced vaporization temperature and vapor attenuation of laser radiation, the melt pool depth increases as the ambient pressure reduces. Whereas, the effect of the ambient pressure on the penetration depth is not noticeable under a critical ambient pressure. As compared with the atmospheric pressure, the sub-atmospheric pressure is in favor of reducing keyhole collapse by increasing the keyhole stability and thus minimizing porosity in the SLM-built products.

6.2. Recommendations

Great progress in numerically understanding the relationship between the processing parameters and mechanical performance of SLM-built products has been achieved. However, due to the complex multi-physics multi-scale process of SLM, there still exists

a considerable gap for exactly predicting the formation of defects within products and thus providing optimized parameters. In terms of numerical modelling, the following issues which are of significance for understanding the underlying physical mechanisms of SLM process are recommended.

The thermo-mechanical behaviours have been widely studied in the SLM process. Whereas, most of the studies focused on the multi-track or multi-layer product with simple geometries. The thermo-mechanical model needs to be extended to predict residual stresses of complex practical products with the consideration of the realistic processing condition. Product-scale modelling can provide realistic and feasible optimized parameters for specific structures. The eventual goal of product-scale modelling is to efficiently evaluate the mechanical performance of realistic printed products.

The liquid-solid interaction is an important phenomenon which includes the interaction of the metal vapor plume, melt pool and powders in the SLM process. The vapor plume flow can give rises to the external force on the melt pool and powders. The interaction between the melt pool and vapor flow is commonly considered by applying a recoil pressure on the gas/liquid interface. Whereas, there is a lack of modelling the vapor flow behaviours. The vapor-driven movement of powders, which can affect the morphology of the melt track and product porosity [6], is scarcely investigated for SLM. Additionally, the liquid-solid interaction is considered by adopting a mushy zone which is treated as a porous medium with a damping force to account for the pressure drop in the melting and solidification process. However, the mechanical interaction of the melt pool and powders has not been reported.

To consider the multi-scale multi-physics SLM process, it is a feasible and efficient approach to develop a unified model by coupling different numerical methods. In a sequentially DEM-CFD coupling model widely used for SLM, the packing process of the powder bed is simulated first and then the distribution of powders is input into the CFD model. However, to consider the interaction of the fluid flow and powders, a fully DEM-CFD coupling model should be developed. In addition, the CFD-FEM coupling model has been used for predicting the porosity of the printed product and improving the thermal model for the stress analysis [140, 210]. To further employ the CFD-FEM coupling model, a damage mechanics analysis such as crack propagation and fatigue prediction can be conducted based on the predicted residual stresses and porosity of SLM-built products.

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