Modelling of beam energy absorbed locally in conduction mode laser metal fusion

Seyyed Mohammad Ali Noori Rahim Abadi, Yongcui Mi, Fredrik Sikström and Isabelle Choquet

Abstract
Fluid dynamics models for laser material processing with metal fusion in conduction mode generally assume a constant absorptivity. This parameter is known to govern the process. However, it used to be pre-set by extrapolating absorptance measurements made at different conditions or adjusted to reproduce experimental bead shapes. In this study a new approach is developed. It consists in predicting the absorptance as a function of local surface conditions, including the surface temperature. The proposed absorptance model is applied to the metal alloy Ti-6Al-4V. It is found that the absorptance of this alloy changes with surface temperature over a wide range of beam incidence angles. Thermo-fluid simulations with tracking of the free-surface deformation are performed for conduction mode beam welding test cases with a Yb fibre laser and different travel speeds. It is found that the absorptivity coefficient commonly used for this process clearly underestimates the absorptance and the melt pool geometry predicted for the process conditions of this study. The computational results are also compared against experimental results and good quantitative agreement of the melt pool depth, width, length, free surface contour geometry, and the curvature of the end depression left after re-solidification at the laser switch-off location is obtained. The results show that the absorptance field predicted depends on the melt pool development stage, on the spatial location within the beam spot, and on the process conditions.

Keywords: absorptance, coefficient of absorptivity, laser material processing, conduction mode welding, melt pool simulation, OpenFOAM

(Some figures may appear in colour only in the online journal)
1. Introduction

Metal fusion with a laser heat source is the operating principle of several material processing methods including cladding, conduction and keyhole mode welding, powder bed additive manufacturing, direct energy deposition additive manufacturing with blown powder or wire, and post-processing such as polishing. A key factor in these processes is the fraction of electromagnetic energy entering in the material surface. This fraction indeed governs the melt pool thermo-hydrodynamic behaviour, thus the bead geometry, the temperature gradients and cooling rates, and in turn the material microstructure and quality.

With respect to the modelling of the beam energy absorption, three main types of computational fluid dynamics (CFD) approaches can be distinguished in laser metal fusion. One of these approaches includes the ray tracing method used to predict multiple Fresnel reflection and absorption when the laser is operated in keyhole mode as intense vaporization then occurs [1]. In this approach the fraction of absorbed beam energy is expressed as a function of the local surface condition through its curvature. This fraction is then given by the local absorptance, $\alpha$, as a function of the beam local incidence angle. A second type of approach does not model the physics of multiple Fresnel reflections, however it accounts for its effect in a simplified way suited to the application, such as through an equivalent absorptance as used in [2]. This approach has been used for laser operated either in keyhole mode [3–5], or in conduction mode with powder bed [6–8] or with blown powder [2, 9]. Finally, the third type is for conduction mode laser metal fusion for which beam rays reflections imply negligible secondary interaction, such as welding [10–12]. In the two last approaches the fraction of absorbed beam energy, that is then named absorptivity coefficient (often denoted $\eta$), usually neglects the local surface condition. The absorptivity coefficient is indeed generally assumed to be uniform and constant when modelling laser metal fusion with negligible or simplified multiple Fresnel reflections. Then, in principle, it corresponds to an averaged or an equivalent absorptance. For simplicity, $\alpha$ is the only symbol used from now to denote the fraction of beam energy absorbed. Recently, Ebrahimi et al [13] highlighted, for constant absorptivity coefficient, the amplification of flow oscillations when numerical discrepancy between the transferred and the effectively absorbed beam power occurs. To avoid such non-physical behaviour they did propose methods for adjusting dynamically the flux of absorbed laser energy so that the computed absorptivity is effectively constant.

The value of the absorptivity coefficient, for given laser wavelength and metal alloy, can vary from study to study. For instance, it can spread as wide as from 0.24 [5], to 0.8 [4], in CFD models applied to the fusion of Ti-6Al-4V with a 1 μm wavelength laser (see supplementary table S1 (available online at stacks.iop.org/JPD/55/025301/mmedia)). In some of the studies the absorptivity coefficient was set from experimental measurements made at a surface usually in solid state. However, during metal fusion the beam mainly interacts with a surface in liquid state. In other studies it was set to fit the computed bead cross section to experimental measurements. A model for predicting the fraction of beam energy absorbed locally would therefore avoid tedious trial and error adjustment of this parameter.

Furthermore, the numerical prediction of the metal fusion process is sensitive to this parameter. Two CFD test cases that differ only through the value of the absorptivity coefficient can exemplify this fact. Consider for instance conduction mode welding of Ti-6Al-4V with a Yb laser (1 kW, top hat beam spot of 0.75 mm radius, and 5 mm s$^{-1}$ travel speed) and the commonly used close mid-range coefficients of absorptivity of 0.34 and 0.4 (table S1). This example leads (see supplementary figure S1) to an absolute deviation of the fully developed melt pool depth that is larger than the absolute deviation of the absorptivity coefficient. The computed melt pool geometry is therefore significantly affected by this parameter. Thus the importance of determining the fraction of absorbed laser beam energy based on physical principles to improve model predictability.

Prokhorov et al [14] reported several chemical and physical factors controlling the fraction of beam electromagnetic energy absorbed in a metal surface. These factors include laser beam characteristics and material surface quality, temperature, and curvature. When modelling the process from a fluid dynamics perspective some of these factors, although important, are basically difficult to model since they correspond to a smaller space scale than the thermo-fluid approach. Examples are small surface and bulk defects, impurities, or thin oxide layer. Other parameters like beam polarization, beam angle of incidence, and material local temperature are in principle more accessible from a thermo-fluid scale.

Therefore, it is the purpose of this study to model locally the fraction of beam electromagnetic energy absorbed in conduction mode metal fusion accounting for more physics accessible at the thermo-fluid scale. For this, the absorptance model presented in section 2 includes the dependence of the polarity of the laser beam, the wavelength, as well as the local curvature and the local temperature of the metal alloy surface. The modelling assumptions are clarified, discussed, and their roles are highlighted sketching main steps of the model derivation. Detailed parts of the derivations that can be collected from former published works are not repeated here. The proposed absorptance model was implemented into a thermo-fluid solver for metal fusion developed in the open source CFD software OpenFOAM®. The thermo-fluid model is briefly presented in section 3.1. Experiments conducted at different travel speeds to provide validation data are described in section 4. In addition to the traditional cross section micrograph images, these experimental data include in-process quantitative measurements of the melt pool top view geometry, as well as of the final crater formed upon solidification after laser switch off. They are used to test the model when investigating, in conduction mode laser metal fusion and at the thermo-fluid scale, the effect of predicting the fraction of beam energy absorbed by the metal as a function of the local state of its surface.
2. Absorptance model

The notations used hereafter are first introduced. In the present context of monochromatic, coherent and highly collimated light, the beam electric and magnetic fields, \( \vec{E}(\vec{r}, t) \) and \( \vec{H}(\vec{r}, t) \), are orthogonal harmonic waves. They are assumed locally plane, of wavelength \( \lambda \), frequency \( \omega \), wave vector \( \vec{k} \) with the norm \( k = 2\pi/\lambda \), and phase \( \phi \). Thus, at time \( t \) and location \( \vec{r} \),

\[
\vec{E}(\vec{r}, t) = \vec{E}_0(\vec{r}) \exp[i(\omega t - \vec{k} \cdot \vec{r} + \phi)].
\]

A similar expression holds for \( \vec{H}(\vec{r}, t) \). Figure 1 shows a sketch of the incident (\( i \)) reflected (\( r \)) and transmitted (\( t \)) wave vectors and the angle \( \theta \) they make with the local normal to the interface at the interaction point. Since the electromagnetic wave is transverse, it is decomposed into p- and s-polarized components \[15\]. The proposed model aims at determining the fraction of power deposited by the incident electromagnetic field in the metal surface as a function of the local conditions defined at the space and time scales of the thermo-fluid problem. This fraction depends on beam and material parameters. With regards to the beam, the main parameters include intensity, wavelength, local angle of incidence, \( \theta_i \), and polarization.

When the process starts, the laser beam operated to fuse metal uses to strike a workpiece surface in solid state, which is at ambient temperature or above if pre-heating is applied. As the laser wavelength is generally in the infrared or in the visible range, the metal is opaque to the beam radiation and it absorbs all the beam energy transferred. Bergström \[16\] did review absorption mechanisms that can take place in solid metals, and grouped them in two categories: external and internal. The external mechanisms are governed by the surface conditions, namely its roughness, the presence of oxides, impurities, and lattice imperfections. The internal mechanisms are governed by the bulk conditions and can include absorption through intraband transitions, interband transitions and impurities. These transition mechanisms depend on the metal energy band structure and temperature, as well as on the beam wavelength \[14\]. If the beam wavelength is large enough, typically of the order of 10 \( \mu \)m and more, the energy delivered to the metal by a beam photon is less than the energy gap between the Fermi energy level and the next conduction band. Interband transitions are thus forbidden. The electro-magnetic field transported by the photons is then used to accelerate the conduction electrons, and as a counterpart the beam wave is damped. The amount of beam energy locally transferred is a function of the electric conductivity \( \sigma_e \) of the conduction electrons in the medium. As the metal alloy temperature is large (i.e. \( T \gg 0 \) K), the mean free path of the metal conduction electrons is smaller than the photons penetration depth and no anomalous skin effect occurs. The local electron current is then proportional to the local electric field \[14\]. If the photon energy is instead larger than the energy band gap, interband transitions can also occur as observed in most metals when the laser wavelength is of the order of 1 \( \mu \)m and less \[14\]. The electromagnetic wave is then also damped due to the bound charges.

Next, the electrons re-distribute the gained energy through collisions with other electrons and with crystal ions vibrating about their fixed position (phonons). The former type of collision leads to the thermalization of the conduction electrons in about ten collisions that require approximately 500 fs or less to take place (see \[17\] and references therein). The second type of collision is less efficient to redistribute the energy as it involves particles of very disparate masses. It implies a longer time, of the order of some pico seconds, for the conduction electrons and the crystal lattice temperatures to relax \[17\]. In laser applications for metal fusion the duration of the material irradiation as well as the characteristic time of the thermo-fluid problem use to be much longer than this relaxation time. As a result electrons and lattice can be assumed at local thermal equilibrium, contrary to laser processing with metal sublimation for e.g. micro-machining or micro-structuring and ablation using ultra-short laser pulse \[18\]. As the temperature rises above the melting temperature the metal crystalline structure breaks down and the band structure vanishes. Subsequently, phonon energy absorption can no longer occur through interband transitions. Also, energy dissipation through collisions with phonons is then replaced by collisions with moving ions \[19\].

For laser fusion applications such as cladding, welding and additive manufacturing the metal located in the irradiated beam spot is solid at the beginning of the process (called hereafter the solid start period) that is for less than a tenth of a second. During the rest of the process, which is often longer than the solid start period, the beam interacts with liquid metal whose surface is then smooth. In the test cases studied hereafter for instance, 50 mm long beads were welded in 10 s or more depending on the travel speed. The simulations led to a solid start time span of \( 7 \times 10^{-3} \) s or a bit less depending on the travel speed. For each travel speed used in this example the rest of the process was therefore more than two orders of magnitude longer than the solid start period. The duration of the transitory beam interaction with solid state metal is
however longer than the typical time step \(\leq 10^{-5} \text{ s}\) used when addressing the fusion process with a CFD approach. Nevertheless, the set of simplifications made in this study to model the absorptance are as follow:

A1: The metal alloy is isotropic.
A2: The metal alloy has linear electromagnetic properties.
A3: The beam magnetic field intensity \(\vec{H}\) and electric field \(\vec{E}\) are sufficiently weak so that non-linear effects are negligible.
A4: Surface (and lattice) impurities and imperfections are neglected.
A5: Material oxidation in the beam interaction spot is supposed to be negligible.
A6: The local surface current density is negligible.
A7: Interband absorption that can occur during the first instants of metal fusion with a laser beam in the visible or near-infrared range while the irradiated material is still in solid state, is assumed to be negligible.

Assumption A1 necessitates negligible metal ion vapour and fumes in the beam path [20]. Fusion in conduction mode is associated with little vaporization while fumes are generally present to some extent. However, they can be moved away from the beam area as often made to permit tracking the process. A consequence of A1 is that the beam energy is not attenuated before reaching the metal surface. Assumption A2 implies that the electric and magnetic permittivity of the medium are scalar functions. This assumption might be inappropriate in e.g. additive manufacturing when re-melting a previously deposited bead if the temperature in the beam spot is very close to the melting temperature so that the material has reminiscence of the anisotropic crystalline structure of the previously deposited bead. Assumption A3 implies that the material should have negligible magnetism. It is not true for important steel alloys that are ferro- and ferrimagnetic, unless the temperature is beyond the Curie temperature from which the collective magnetic interaction is lost and the material behaves as a paramagnetic material. Assumption A4 uses to be satisfied by lasers operated for metal fusion (i.e. continuous wave or ns-pulsed, up to multi-kW power focussed into a spot of few mm-size). However it might not be satisfied with ultrashort-pulse MW-laser focused into a few \(\mu\text{m}\) spot to reach material sublimation. Assumptions A5, A6 and A7 mean that physics taking place at space scales smaller than the thermo-fluid scale is neglected, although the space-scale of this physics might be of same order as the beam wavelength. A consequence of these simplifications (see e.g. S3 and S4 below) is to reduce a real surface to an ideal surface and therefore to simplify diffuse or mixed specular-diffuse beam reflection to specular reflection. Furthermore, the presence of an oxide layer on the material surface would also change the absorptivity. Assumption A6 is thus more critical. It can however be verified with appropriate surface preparation and shielding of the laser-material interaction zone or operating in a chamber with controlled atmosphere. Finally, taking into account the brevity of the transient beam interaction with the metal in solid state, the simplification A8 is most probably justified.

Key steps S1–S8 of the model derivation are now briefly recalled to show at which stage the assumptions are used. The first steps can be based on the system of equations constituting the Maxwell equations in a medium.

S1—In the atmosphere, with assumption A1 the Maxwell equations reduce to Maxwell equations in vacuum and the beam wave frequency expresses:

\[
\omega = 2\pi c/\lambda,
\]

where \(c\) is the speed of light in vacuum. In the metal, using the assumptions A2–A4 the Maxwell equations in a medium can be closed with the following constitutive relations,

\[
\vec{M} = 0, \quad \vec{P} = \epsilon_0 \chi_r \vec{E}, \quad \vec{j}_e = \sigma_\ell \vec{E}
\]

defining the magnetization field, \(\vec{M}\), the polarization field, \(\vec{P}\), and the current density, \(\vec{j}_e\). The notation \(\epsilon_0\) holds for the permittivity of vacuum, \(\chi_r\) the material’s electric susceptibility, and \(\sigma_\ell\) the material’s electric conductivity. Therefore, the magnetic displacement induction reduces to \(\vec{B} = \mu_0 \vec{H} = \mu_0 (\vec{M} + \vec{H})\), where \(\mu_0\) is the permeability in vacuum. The electric displacement induction becomes \(\vec{D} = \epsilon_0 \vec{E} + \vec{P} = \epsilon \vec{E}\) where \(\epsilon = \epsilon_0 (1 + \chi_r)\) is the permittivity of the metal alloy. As metals are damping media for electromagnetic waves, the permittivity is complex, \(\epsilon = \epsilon' + i \epsilon''\). The related refractive index, \(n_\ell\), that is defined as \(n_\ell^2 = \epsilon / \epsilon_0\) is also complex.

S2—Using relations in S1, Maxwell–Faraday’s law applied to the travelling waves \(\vec{E}\) and \(\vec{H}\) implies that the waves amplitudes are related through \(\mu_0 \omega_\ell H_0 = k_\ell E_0\).

S3—the electric field boundary condition derived from Gauss’s law at the interface between atmosphere and metal alloy for a p-polarized wave using relations in S1, and assuming a locally flat interface (A5, A6) with no surface current density (A7), implies that (a) the wave frequency of the incident, reflected and transmitted waves are equal, (b) the wave reflection is specular, \(\theta_i = \theta_r\), and (c) the refraction obeys Snell’s law, \(\sin \theta_i = n_s \sin \theta_s\). The notations are defined in figure 1.

S4—Considering instead a s-polarized wave, the magnetic field boundary condition derived at the interface from Gauss’s law of magnetism using the relations in S1, and assuming a locally flat interface (A5, A6) lead to the same conclusions S3(i)–(iii).

S5—For either a p- or s-polarized wave, the fields boundary conditions derived from Maxwell–Faraday’s and Ampères’ laws at the metal surface assumed locally flat (A5, A6) with no surface current density (A7), combined with relations in S1, lead to the following conditions:

\[
E_{\ell, \text{tan}} + E_{r, \text{tan}} = E_{t, \text{tan}}, \quad \text{and} \quad H_{\ell, \text{tan}} + H_{r, \text{tan}} = H_{t, \text{tan}},
\]

on the tangential components of the incident, reflected and transmitted electric and magnetic fields at the interface.

S6—Applying the interface conditions of S5 to the s- and p-polarized waves, and using the relations of S2–S4 to re-arrange
the expressions, the respective fractions of reflected electric field write:

\[ r_s = \frac{(E_r)}{(E_t)} = \frac{\cos \theta_1 - \sqrt{n_e^2 - \sin^2 \theta_1}}{\cos \theta_1 + \sqrt{n_e^2 - \sin^2 \theta_1}}, \]  
\[ r_p = \frac{(E_r)}{(E_t)} = \frac{-n_e^2 \cos \theta_1 + \sqrt{n_e^2 - \sin^2 \theta_1}}{n_e^2 \cos \theta_1 + \sqrt{n_e^2 - \sin^2 \theta_1}}, \]  

with the refractive index \( n_e = n'_e + in''_e \). The real part \( n'_e \) is the ratio of the phase velocity of light in the metal alloy to the speed of light in vacuum. The imaginary part \( n''_e \) is the extinction coefficient, related to the damping of the wave.

\[ \text{S7—As the incident and reflected waves propagate in the same medium, the Fresnel equations of S6 lead to the fraction } R \text{ of reflected power for each polarization wave, } R_s = r_s r'_s \text{ and } R_p = r_p r'_p. \] The star denotes the complex conjugate.

According to the principle of energy conservation the fraction \( A \) of beam power absorbed by the metal, is \( A_s = 1 - R_s \) and \( A_p = 1 - R_p \). Introducing \( a \) and \( b \) such that \( ab = n'_e n''_e \) and \( a^2 - b^2 = n'_e^2 - n''_e^2 - \sin^2 \theta_1 \), after some calculations it results in the absorbance for s- and p-polarization waves:

\[ A_s = 1 - \frac{(\cos \theta_1 - a)^2 + b^2}{(\cos \theta_1 + a)^2 + b^2}, \]  
\[ A_p = 1 - \frac{(a - \sin \theta_1 \tan \theta_1)^2 + b^2}{(a + \sin \theta_1 \tan \theta_1)^2 + b^2} \left(1 - A_s\right), \]

where

\[ a^2 = \frac{1}{2} \left[ n'_e^2 - n''_e^2 - \sin^2 \theta_1 \right. \]
\[ + \sqrt{\left(n'_e^2 - n''_e^2 - \sin^2 \theta_1\right)^2 + 4 n'_e^2 n''_e^2}, \]  
\[ b^2 = \frac{1}{2} \left[ -n'_e^2 + n''_e^2 + \sin^2 \theta_1 \right. \]
\[ + \sqrt{\left(n'_e^2 - n''_e^2 - \sin^2 \theta_1\right)^2 + 4 n'_e^2 n''_e^2} \].

In metal fusion applications the laser beam is often unpolarized or circularly polarized. Then, an equal amount of power is deposited by the s- and p-polarization waves into the metal surface, so that the fraction of absorbed power can be calculated as:

\[ A = \frac{1}{2}(A_s + A_p). \]

\[ \text{S8—Expressions for the refractive index } n'_e \text{ and the extinction coefficient } n''_e \text{ are now needed to close the model. They depend on the material ability to store energy under the influence of an electric field } (\epsilon, \text{ see S1}), \text{ therefore on the laser wavelength, the local temperature, and the material local characteristics. With the assumptions A2–A6 and A8, the Drude model for free electrons moving under the action of the wave alternating electric field can be used to determine } n'_e \text{ and } n''_e \text{ as} \]
\[ n'_e = \frac{1}{\sqrt{2}} \left[ \left(1 + Q^2\right)^{1/2} - Q + 1\right]^{1/2}, \]  
\[ n''_e = \frac{1}{\sqrt{2}} \left[ \left(1 + Q^2\right)^{1/2} + Q - 1\right]^{1/2}, \]  
\[ Q = \frac{\omega_p^2}{\omega_k^2} \left(\sigma_e/T\right)^{1/2} \text{,} \]  
\[ \tau = \frac{m^* \sigma_e(T)}{N_e^2}, \]

where \( \omega_p \) is the plasma frequency, \( \tau \) the electron relaxation time, \( N \) the free electron density, \( e \) the electron charge, and \( n^* \) the optical effective mass of a conduction electron [22, 23]. The dependencies to the beam wavelength and surface temperature are included through \( \omega_k \) (see equation (2)) and the material electrical conductivity, \( \sigma_e \), respectively.

### 3. Thermo-fluid model

A one-fluid, unsteady and three dimensional approach is used to model the metal in both solid and liquid states with melting and re-solidification, and the atmosphere. The fluids are treated as immiscible, mechanically incompressible, newtonian, and the flow is assumed to be laminar. Model assumptions were further discussed in a former study [24]. The deformation of the liquid metal free surface is tracked, and as the laser is operated in conduction mode metal vaporization [25] and multiple-Fresnel reflection [26] are expected to be negligible. The dependence of the thermodynamic, transport and electric properties to temperature are included. Since the liquid metal has a very low Prandtl number and the order of the velocity magnitude is small the effect of viscous dissipation is ignored [27].

#### 3.1. Governing equations

In this work the free surface deformation is tracked with the volume of fluid (VOF) multiphase flow approach [28]. The transport equation governing the metal volume fraction, \( \alpha \), writes:

\[ \frac{\partial \alpha}{\partial t} + \nabla \cdot (\alpha \vec{U}) + C_{\alpha} \nabla \cdot [\alpha \beta (1 - \alpha) \vec{U}] = 0, \]

where \( t \) is the time, \( \vec{U} \) the one-fluid velocity vector, \( C_{\alpha} \) the compression factor, and \( \vec{U} \) the compression velocity. The third term of equation (16) was introduced by Berberovic et al...
[29] to sharpen the free surface capturing. It is only effective at the interface due to the factor $\alpha(1-\alpha)$. In this study $C_\alpha = 1$ (to verify conservation).

The one-fluid continuity, momentum, and energy equations are given by:

$$\frac{\partial \rho}{\partial t} + \nabla \cdot (\rho \vec{U}) = 0, \quad (17)$$

$$\frac{\partial (\rho \vec{U})}{\partial t} + \nabla \cdot (\rho \vec{U} \vec{U}) + \frac{1}{\rho} \nabla \cdot \nabla \cdot (\rho \vec{U}) \vec{U} = -\nabla \rho + \rho \left[ 1 - \beta(T - T_m) \right] \vec{g} + \vec{F}_S + \vec{S}_D, \quad (18)$$

where $\rho$ is the one-fluid density, $\vec{p}$ the gauge pressure, and $T$ the temperature. $\mathbb{I}$ denotes the identity matrix. The one-fluid density, dynamic viscosity, $\mu$, specific heat capacity at constant pressure, $c_p$, and thermal conductivity, $k$, are defined in the next section. In the body force due to weight and buoyancy in the liquid alloy, $\vec{g}$ is the gravitational acceleration, $\beta$ is the volume expansion coefficient, and $\rho_m$ is the working material density at the melting temperature $T_m = 0.5(T_s + T_l)$, where $T_s$ and $T_l$ are the solidus and liquidus temperatures, respectively. The momentum source term $\vec{F}_S$, which is described in more details in [24], includes the capillary and the thermocapillary forces. It is written as:

$$\vec{F}_S = \left[ \sigma \kappa \vec{n} + \frac{\partial \sigma}{\partial T} \right] \left( \nabla T - \vec{n} (\nabla T) \right) \left| \nabla \alpha \right| \frac{2 \rho}{\rho_1 + \rho_2}, \quad (20)$$

where

$$\vec{n} = \frac{\nabla \alpha}{\left| \nabla \alpha \right|}, \quad (21)$$

$$\kappa = -\nabla \cdot \vec{n}, \quad (22)$$

are the unit vector locally normal to the free surface and the interface curvature, respectively. The last factor in the right-hand side of equation (20) is a multiplier introduced by Brackbill et al [30] so that surface tension acceleration remains independent of density when the densities of the fluid phases differ by several orders of magnitudes. The solid-liquid transition region is modelled using the isotropic Blake–Kozeny model [31] to damp the fluid alloy velocity down to the solid velocity with the Darcy term that writes:

$$\vec{S}_D = -C_D \left( \frac{1-f_s}{f_s^2} \right)^2 \vec{U}, \quad (23)$$

where $C_D$ is a large constant ($=10^4$) that is inversely proportional to the permeability of the porous medium, whereas $\epsilon_D$ is a small constant ($=10^{-3}$) to avoid division by zero in the solid phase. The liquid fraction, $f_s$, is a continuous function of temperature that increases from zero in the solid state ($T \leq T_j$) to one in the liquid state ($T \geq T_j$). It is described by the following function with continuous derivative [32]:

$$f_s = \frac{1}{2} \left[ \text{erf} \left( \frac{4(T - T_m)}{T_l - T_s} \right) + 1 \right]. \quad (24)$$

In the right-hand side of the energy conservation equation $h_{fl}$ is the latent heat of fusion. As the studied metal is an alloy, the convection of latent heat is also included. In the interface radiative cooling to the surrounding atmosphere,

$$\dot{Q}_{\text{rad}} = c_{\text{rad}} \rho (T^4 - T_{\text{ambient}}^4) \left| \nabla \alpha \right| \frac{2 \rho}{\rho_1 + \rho_2}, \quad (25)$$

where $c_{\text{rad}} = 5.67 \times 10^{-8}$ W (m$^2$ K$^4$)$^{-1}$ is the Boltzmann constant, $c_{\text{rad}}$ the radiative emissivity, and $T_{\text{ambient}} = 300$ K the ambient temperature. This radiative cooling is only effective on the interface due to the term $\left| \nabla \alpha \right|$. The last factor in the right-hand side is the same multiplier as in equation (20). Finally, the last term of equation (19), $\dot{Q}_{\text{laser}}$, is the rate of heat input due to the laser beam. It is modelled through a volumetric source term that is only effective at the interface, according to:

$$\dot{Q}_{\text{laser}} = A \dot{q}_{\text{laser}} \left| \nabla \alpha \right| \frac{2 \rho}{\rho_1 + \rho_2}, \quad (26)$$

where the absorptivity coefficient is defined as the local absorbance $A = A(\lambda, \theta, T)$ in equations (6)–(15). It is a function of the beam wavelength, and the local variables that are the beam incidence angle, $\theta = \theta_0 - \kappa, \vec{n}$, and the material temperature. In this study, the heat flux coming from the laser beam is set based on experimental measurements corresponding to a top-hat distribution:

$$\dot{q}_{\text{laser}} = \frac{P_{\text{laser}}}{\pi r_{\text{laser}}^2}, \quad (27)$$

where $P_{\text{laser}}$ is the laser total power, and $r_{\text{laser}}$ the laser spot radius. This heat source is moved at the travel speed $U_{\text{laser}}$ along the positive direction $x$.

### 3.2. Material properties

The metal is the titanium alloy Ti-6Al-4V and the atmosphere is made of argon gas to avoid oxidation. In the temperature range of this study, the properties of this rare gas have a weak dependence to temperature, see e.g. the specific heat capacity in [33]. The properties of the shielding gas are thus set constant at 300 K and reported in table 2. The properties of the Titanium alloy Ti-6Al-4V are presented in table 1. For con-


Table 1. Material properties of solid and liquid Ti-6Al-4V [34–36].

| Parameter | Value  | Validity range in K | Dimension  |
|-----------|--------|---------------------|------------|
| T_l       | 1873   | —                   | K          |
| T_f       | 1923   | —                   | K          |
| T_m       | 1898   | —                   | K          |
| ρ1, ρm    | 4420.0 | —                   | kg m⁻³     |
| μ1        | 0.0035 | —                   | kg (m s)⁻¹|
| cₚ,₁,₁    | 483.04 + 0.215 T | 298 ≤ T < 1268 | J (kg K)⁻¹ |
| cₚ,₁,₁,₁  | 412.7 + 0.1801 T | 1268 ≤ T ≤ T_m | J (kg K)⁻¹ |
| k₁,₁      | 830.0  | T_m ≤ T < 3000     | W (m K)⁻¹  |
| k₁,₂      | 1.2595 + 0.0157 T | 298 ≤ T < 1268 | W (m K)⁻¹  |
| k₁,₃      | 3.5127 + 0.0127 T | 1268 ≤ T ≤ T_m | W (m K)⁻¹  |
| σ         | σ₀ + δσ/δT (T − T_m) | T_m ≤ T < 3000 | N m       |
| h_f       | 2.86 × 10⁵ | —                   | J kg⁻¹     |
| β         | 8.0 × 10⁻⁶ | —                   | 1/K        |
| εₑₑₑdle   | 0.1536 + 1.8377 × 10⁻⁴ (T − 298) | 298 ≤ T < 3000 | —          |

Table 2. Material properties of argon gas [33].

| Parameter | Value  | Dimension  |
|-----------|--------|------------|
| ρ₂       | 1.6337 | kg m⁻³     |
| μ₂       | 42.26 × 10⁻⁵ | kg (m s)⁻¹ |
| cₚ,₂     | 520    | J (kg K)⁻¹ |
| k₂       | 0.0177 | W (m K)⁻¹  |

The indices 1 and 2 stand for the primary (Ti-6Al-4V) and the secondary (argon gas) phases, respectively. The properties of the metal alloy and shielding gas are used as defined in tables 1 and 2, respectively. No artificial enhancement of the transport coefficients (discussed by Ebrahimi et al [13]) is applied in this study.

The coefficients entering the surface tension defined in table 1 have the following values: σ₀ = 1.6 N m⁻¹ and δσ/δT = −2.6 × 10⁻⁴ N (m K)⁻¹ [37]. The ratio of free electron density to the optical effective mass of a conduction electron in the alloy Ti-6Al-4V is N/m* = 2 × 1.36 × 10²⁰ g cm⁻³ as determined by Wieting and Schriempf [38]. The electrical conductivity, σₑₑₑ(T), of Ti-6Al-4V is plotted in figure 2. It is based on volume corrected experimental measurements of the electrical resistivity as a function of temperature by Boivineau et al [34].

3.3. Computational domain and boundary conditions

Figure 3(a) displays the real workpiece. Three identical plates with welds approximately along the centreline can be seen side by side. The travel speed is the only process condition that is varied. Two computational domains are used for the numerical simulations. Figure 3(b) shows the narrow domain with its dimensions. The wider domain has the same width (along y) as the experimental plates. Since the experimental observations indicate that the melt pool reaches to a stable condition in less than 1.5 cm, the maximum simulated welding length is set to 1.8 cm, and the computational domains are spanned to 2 cm in the x direction. It was checked that the computed solution was kept unchanged until laser switch-off when using these two domains. However, some variations in the solution

![Figure 2. Variations of Ti-6Al-4V electrical conductivity with respect to temperature.](image-url)
were observed during the cooling stage after laser switch-off, as shown in a next section. Due to symmetry only half of the plate is simulated. The boundary conditions for $U$ (velocity), $T$ (temperature), $p$ (pressure), and $\alpha$ (volume fraction) are also reported in figure 3(b). They apply to both computational domains.

3.4. Solution method

The absorptance model was implemented in OpenFOAM® completing the melt pool solver developed in former studies (see e.g. [24]). The pressure-velocity coupling was computed using a pressure-implicit with splitting of operators algorithm. Gauss linear scheme that is a second order was applied for handling all the convective terms, except for the VOF-equation. In that last case Gauss van Leer scheme was used, which strictly bounds the volume fraction value between zero and one. The diffusive fluxes were discretized by central differencing scheme. The volume fraction transport equation was solved by MULES (Multidimensional universal Limiter with Explicit Solution) algorithm to guarantee boundedness of its solution.

To decrease the computational cost and yet maintain the accuracy of the solution, the adaptive mesh refinement (AMR) method was applied [39]. The refinement process is active in the melt pool area. The grid is the finest in the liquid phase
was guided through an 800 laser system (mod. YRL-6000-S) with a 1070 nm wavelength.

The laser generated by a 6 kW IPG Ytterbium fiber to manipulate the welding tool from Permanova Lasersystem, a CNC gantry from Isel. An ion system.

4.1. Robotized welding setup

A CNC gantry from Isel® Germany (mod. M40) was used to manipulate the welding tool from Permanova Lasersystem AB. The laser generated by a 6 kW IPG Ytterbium fiber laser system (mod. YRL-6000-S) with a 1070 nm wavelength was guided through an 800 µm core diameter optical fibre to the tool. The welding tool comprised a 160 mm focal length collimator and a 300 mm focal length focusing lens. It was mounted with an angle of 5 degree with respect to the workpiece to avoid the back reflections of the beam into the tool. The focused beam had a Rayleigh length of 12.2 mm and a spot in the focal plane with a diameter of 1.5 mm. A chamber was built to provide a consistent argon environment with an oxygen level below 0.2%. Bead-on-plate welding with different travel speeds of 5, 7.5 and 10 mm s⁻¹ was investigated. Each plate was made of Ti-6Al-4V with dimensions 300 mm × 40 mm × 3.2 mm. The plates were cleaned with acetone prior to welding. The laser beam with 1 kW continuous wave power was consistently focused on the surface of the workpieces to obtain conduction mode welding. The power distribution in the beam spot, measured by Primes GmbH beam profiler, was almost uniform. Therefore, a top-hat distribution was assumed in the computations. The length of each weld was 50 mm and repeated three times for each travel speed. The topography of the solidified end depression formed at the location of the laser beam switch-off was also acquired with a white light interferometric profilometer from Filmetrics that has a resolution of 0.92 µm. The samples were cross sectioned in the middle of each weld, polished and etched with 8% fluoroboric acid solution prior to macroscopic investigation.

4.2. Image acquisition setup

Melt pool images were captured from a top view during welding with a CMOS camera (IDT CCM-1540) and an imaging lens (f = 150 mm) integrated coaxially with the beam in the welding tool. With this setup the spatial resolution of the vision system was around 23 µm. The frame rate was 200 frames s⁻¹ and the pixel width and height were 480 × 424.

Three LEDs were posed with more than 70 degrees angle with respect to the workpiece, as shown in figure 5(b), providing a bright field for the image acquisition. The camera and the LEDs were triggered synchronously such that the LEDs could operate at an overdrive current (10A) to provide a high light intensity and an acquisition with a short exposure time of 45 µs. Within this exposure time, the displacement during acquisition was 0.45 µm at the travel speed of 10 mm s⁻¹, much less than the resolution of the vision system. It means that motion blurring was eliminated. To minimize plasma interference and reduce disturbance caused by smoke, a spectral band pass filter (central wavelength at 450 nm and a full width-half maximum of 10 nm) matched to the LED wavelength was used in the optical path of the camera.

To acquire the melt pool free surface geometry, the relation between the image coordinate frame and the real object in the world coordinate frame needs to be known. As the welding tool was used with an angle of 5 degree with respect to the workpiece surface, the imaging system was calibrated using Zhang’s method [40] to obtain accurate quantitative geometry measurements. Using the calibration results, an uncertainty of 0.05 pixels (23 × 0.05 ≈ 1 µm) was obtained.

Figure 4. Illustration of the adaptive mesh refinement in the melt pool region (top-view) and its close vicinity with the plot of the liquid alloy volume fraction, \( f_l, f_s = 1 \): liquid melt pool, \( 1 < f_l < 0 \): mushy zone, \( \beta = 0 \): solid alloy.
4.3. Melt pool top surface measurement

The imaging setup described in section 4.2 resulted in melt pool free surface images with the liquid metal boundary contour at the quasi-steady welding condition clearly detectable. The thickness of the transition zone at the top surface showing up as the contour between the solid and liquid metal is in the same order as the fine grid size in the simulations i.e. 0.05 mm. Images acquired in the beam reference frame were processed according to the following main steps to measure the dimensions of the contour. The Canny edge detection method (a multi-stage algorithm using intensity gradients) was applied to detect edges in each image [41]. Edge points located in the lateral, front and rear part of the melt pool contour were then automatically detected by an algorithm as explained by Mi et al [42]. Examples of processed images are shown in figure 6. The mean melt pool image (a) is a stack of multiple images on top of each other followed by an averaging of them. The detected edges (b) is a typical example of a binarized image from the Canny method. The melt pool contour (c) is the detected contour based on edge detection and further image processing steps.

The detected melt pool contour and feature points were then transformed to the world coordinate frame to enable measurements of the free surface width and length. A statistical variation in the measurements is explained by natural variations in the process (significant), uncertainty in the camera calibration method (insignificant) and detection errors in the feature extraction algorithm (significant). Taking into account this variation the measured dimensions are presented with an estimated combined uncertainty of ±0.1 mm. Following the same procedure, the geometry was measured at the different welding travel speeds. The melt pool width and length measurements are reported in table 3.

At the end of the welding, re-solidification resulted in a depression at the location of the laser beam switch-off. The three-dimensional topology of the end-process depression was measured on three samples for each process condition. Examples of these measurements can be seen in the supplementary figure S2. The mean depth of the concavity measurements for $U_{\text{laser}} = 5, 7.5$ and 10 mm s$^{-1}$ was 0.24, 0.21 and 0.19 mm, respectively. The corresponding uncertainty in the measurements were ±0.02, ±0.01 and ±0.005 mm. These results show that the depth of the end point concavity is decreased when the travel speed is increased. This feature is related to the melt pool depth during welding. The profiles of the depression surface that were extracted along and transverse to the welding direction are plotted in figure 12 (red colour). Furthermore, the mean weld bead height upstream the location of the laser switch-off (i.e. in the quasi-steady region) were 0.09 mm (case $U_{\text{laser}} = 5$ mm s$^{-1}$), 0.06 mm (7.5 mm s$^{-1}$), and 0.05 mm (10 mm s$^{-1}$), with the same uncertainty as the depth measurements.

5. Results and discussions

The computational results presented hereafter correspond to the experimental conditions with the circularly polarized Yb fibre laser described in section 4.1. The absorptance defined
as a function of the local angle of incidence and the local temperature at the workpiece surface was thus calculated using the equations (10), (6)–(9), and (11)–(15). Besides, the test cases were also computed assuming a constant absorptance for comparison purpose.

5.1. Absorptance, recoil pressure

Figure 7 shows the absorptance calculated for a circularly polarized beam as a function of the angle of incidence for various beam wavelengths and temperatures at a Ti-6Al-4V surface [38–46]. Experimental and semi-empirical data available in the literature are also reported in the figure. They were measured at room temperature and normal beam incidence using CO$_2$ laser of wavelength $\lambda = 10.6$ µm [38, 43], Deuterium fluoride chemical laser of wavelength $\lambda = 3.8$ µm [38], and Nd:YAG laser of wavelength $\lambda = 1.06$ µm [44–46]. Data points obtained at $T = 300$ K from the semi-empirical expressions, valid at normal incidence, of Hagen–Roben and Bramson are also reported in the figure [47, 48]. These different sources show some data spreading. Anyway, the absorptance calculated at the different wavelengths lies within the range of these few available data points. The plots show, as expected, that the absorptance varies weakly with the beam incidence angle when $\theta_i \leq 30^\circ$, and sharply when $\theta_i \geq 60^\circ$.

Figure 8 shows the variation of the absorptance with respect to Ti-6Al-4V surface temperature for various angles of incidence of a 1.06 µm wavelength beam.
the conduction to the keyhole welding mode. Well below the threshold value (with respect to the ambient pressure is about 0.29, which is the vaporisation enthalpy ($\Delta h_{fg}$), and $M$ the molar mass of Ti-6Al-4V ($\approx 0.446$ kg mol$^{-1}$), $T_v$ the vaporisation temperature ($\approx 3315$ K [5]). As expected, the computed values of $P_r$ are maximum at the lowest travel speed studied that is $U_{laser} = 5$ mm s$^{-1}$. Figure 9 shows the computed temperature field and the resultant recoil pressure field on the melt pool free surface at $U_{laser} = 5$ mm s$^{-1}$. It can be seen that the free surface temperature is everywhere below the vaporization temperature. The maximum ratio of the recoil pressure with respect to the ambient pressure is about 0.29, which is well below the threshold value ($\approx 0.6$ [49]) for the transition between the conduction to the keyhole welding mode.

5.2. Comparison to experimental measurements

To establish the model reliability the simulation results are now confronted to experimental data. Figure 10 compares for each travel speed the metallographic image of the post-solidified melt pool in a section transverse to the travel direction (left side) to the computed isotherms (right side). The computational data are plotted at time $t = 3$ s when $U_{laser} = 5$ mm s$^{-1}$ and at time $t = 1.6$ s when $U_{laser} \geq 7.5$ mm s$^{-1}$. At these times the melt pool has reached the fully developed condition and is at quasi-steady state in the beam reference frame. The white line interface visualizes the predicted deformation of the melt pool free surface. This upper surface, which changes shape upon solidification, is discussed with a next figure. In the left micrograph images three zones can be recognized. The fusion zone (FZ) with its large grain size, the heat affected zone with smaller grains (HAZ), and the unaffected zone (UZ). An evaluation of their edge is highlighted with dashed lines. In the facing simulation plots the FZ is delimited from the HAZ by the solidus isotherm. For the studied alloy the isotherm delimiting the HAZ from the UZ is somewhere between 600 and 900 K below the solidus temperature, depending on the cooling rates. The $\alpha \rightarrow \beta$ transition temperature isoline, $T_{transition} (=1268$ K, [35]) can thus be used to evaluate its location. For each travel speed this qualitative comparison shows a good agreement between size and location of the different computed and experimental zones. It can also be seen, as expected, that the increase in the travel speed decreases the areas of the FZ and HAZ due to the lowering of the heat input per unit length.

Quantitative comparisons are now made including the third direction, i.e. the travel direction. Figure 11 shows for each travel speed the predicted and the experimentally measured melt pool free surface contour at quasi-steady condition. The computed contour is determined as the intersection of the solid/liquid isosurface $f_1 = 0.5$ and the metal/atmosphere isosurface $\alpha = 0.5$. The uncertainty of the computed contour data is evaluated based on the mesh cell-size in the liquid region to be equal to $\pm 0.05$ mm. The comparisons of figure 11 confirm that, for each of the three travel speeds, the model predicts well the melt pool width and length with overlap of the error bars. It can also be seen that the model predicts well the melt pool contour geometry apart from the small deviation observed at the pool rear when $U_{laser} = 5$ mm s$^{-1}$.

Table 3 presents the measured melt pool dimensions in the three space-directions based on both the numerical and the experimental data. It is recalled that due to symmetry only half of the plate was simulated. The data reported in table 3 are as measured, thus the numerical half-width. It can be seen that in general, the predicted results are in good agreement with the experimental data. The maximum deviation of 11.1% is reported for the melt pool width at $U_{laser} = 10$ mm s$^{-1}$. A possible reason for the deviation might be due to the fact that the laser heat source used in the experiments did not provide an ideal top-hat power distribution. In particular the beam power density distribution was observed to be a bit curvy rather than completely sharp at the spot edge.

Figure 9. Temperature (upper half) and recoil pressure fields (lower half) on the free surface of the melt pool at the travel speeds $U_{laser} = 5$ mm s$^{-1}$. 

\[ P_r(T) = 0.54P_{ambient} \exp \left( \frac{h_{fg}M}{R} \left( \frac{1}{T_v} - \frac{1}{T} \right) \right). \]
Figure 10. Cross section metallographic image of the solidified melt (left) and computed temperature field in developed melt pool at quasi-steady state (right). (a) $U_{\text{laser}} = 5 \text{ mm s}^{-1}$, (b) $U_{\text{laser}} = 7.5 \text{ mm s}^{-1}$, (c) $U_{\text{laser}} = 10 \text{ mm s}^{-1}$. All the images are at the same scale.

Figure 12 shows for each travel speed a comparison between the predicted and the measured surface profile of the end depression. Two computational results are visualized on each figure. One is obtained with the small width domain (figure 3(b)) while the wider computational domain reproduces the width of the experimental plates. The experimental measurement uncertainties are $\pm 0.020 \text{ mm}$ when $U_{\text{laser}} = 5 \text{ mm s}^{-1}$, $\pm 0.010 \text{ mm}$ at 7.5 mm s$^{-1}$, and $\pm 0.005 \text{ mm}$ at 10 mm s$^{-1}$. For clarity the error bars of the numerical predictions, which are larger ($\pm 0.05 \text{ mm}$), are not reported in the figures. Accounting for the data uncertainties the calculation results obtained with the narrow domain show satisfying agreement with the experimental measurements. It can also be seen that during the cooling stage the width of the computational domain has little effect on the results at the highest travel speed. However discrepancies are observed at the lower
travel speeds when reducing the domain width. The discrepancy increases when the travel speed decreases since the heat has then more time to diffuse. Finally, the calculation results obtained with the computational domain as wide as the experimental samples are in good agreement with the experimental measurements.

5.3. Comparison with constant coefficient of absorptivity

Conduction mode laser beam welding simulations with a fluid dynamics approach used to assume a constant coefficient of absorptivity (see e.g. table S1). For Ti-6Al-4V this coefficient is often set to 0.34. The test cases of this study were thus also computed making the same standard assumption. The melt pool cross section predicted at \( U_{\text{laser}} = 5 \text{ mm s}^{-1} \) is plotted in figure S1. The resultant melt pool dimensions do underestimate the experimental measurements with an absolute deviation of about 17\% for both the depth and length and 27\% for the half-width. These deviations are significantly larger than those reported in table 3 for the computations at \( U_{\text{laser}} = 5 \text{ mm s}^{-1} \) with the absorptance model of section 2. Therefore, if the absorptivity coefficient can be approximated by a constant value, for the studied test cases this value is not 0.34.

Figure 13 visualizes the predicted temperature, electric conductivity, angle of incidence and absorptance in the laser spot and its close vicinity at the melt pool free surface. These fields, plotted at quasi-steady state, were computed applying the thermo-fluid model with the absorptance model presented in section 2. The plots of figure 13 are thus complementary outputs of the test cases addressed in section 5.2. They are presented at the travel speeds \( U_{\text{laser}} = 5 \text{ mm s}^{-1} \) (figure 13 left side) and \( U_{\text{laser}} = 10 \text{ mm s}^{-1} \) (figure 13 right side). The black circular contour visible in each sub-figure represents the edge of the laser spot within which the laser beam energy absorption is effective. It can be observed that the temperature distributions present some differences when the process conditions are changed. For instance, the isotherm at 2850 K is more extended and the maximum temperature is higher when the travel speed is \( U_{\text{laser}} = 5 \text{ mm s}^{-1} \) compared to \( U_{\text{laser}} = 10 \text{ mm s}^{-1} \). These expected differences are reflected by the electric conductivity \((\sigma_i)\) of the alloy in the beam spot that decreases when the temperature increases, as known for this temperature range according to figure 2. Figure 13 shows also that, at the front side of the spot, the local angle of incidence of the beam \((\theta_i)\) increases with the travel speed. Its value, which does not exceed 20\%, is within the expected range for conduction mode laser beam welding. It can also be seen that the absorptance \((A)\) distribution is modified when the travel speed is increased, especially at the spot front area. Referring to figures 7 and 8, these local variations of the absorptance are mainly attributed

![Figure 11](image-url)
to the temperature variations. It is also observed that for the process conditions of this study the absorptance value in the laser spot spreads over a rather narrow range. Based on these results, it is assumed that if the coefficient of absorptivity can be approximated by a uniform value, it can be set to $A = 0.38$, which is a reasonable approximation for the studied test cases. This value is more than 10% larger than the 0.34 generally assumed in conduction mode welding. Looking at figure 13, it can be noticed that $A = 0.38$ corresponds to the maximum of the absorptance plotted at quasi-steady condition in the beam spot rather than to an average value. A little overestimation of the melt pool dimensions might therefore be expected with
Figure 13. Contours of temperature, electric conductivity, angle of incidence and absorptance at the melt pool free surface for $U_{\text{laser}} = 5$ and 10 mm s$^{-1}$. The solid black line visualizes the edge of the top hat power density distribution in the laser spot.

uniform $A = 0.38$ compared to the variable absorptance model (section 2).

Figure 14 compares for each travel speed the melt pool geometry predicted using the local absorptance model (variable $A$) and a constant absorptivity coefficient set to 0.38. The pictures on the left side show free surface contours. The right side images present transverse cross sections passing through the laser beam centre with plots of the solid/liquid isosurface $f_l = 0.5$ (lower part) and the metal/atmosphere isosurface $\alpha = 0.5$ (upper part). As expected for these welding conditions, the profiles computed with the variable and the constant $A$ are close to each other. Inspecting the computational data in the 3-space dimensions it is observed that the melt pool maximum depth, width, and length reach the same values with both models. It can also be seen in most of the 2-dimensional representations of figure 14 that at some places the melt pool computed with constant $A$ is smaller than with the absorptance predicted locally. As $A = 0.38$ is the maximum value of the $A$-range visualized in figure 13, this result might seem curious. The computed amount of laser beam power that effectively entered the workpiece during the simulations was therefore checked. The total power supplied by the laser in the simulated half workpiece is here $0.5 \ A \ P_{\text{laser}} = 190$ W when a constant absorptivity of 0.38 is assumed. The computational results show that at quasi-steady condition the beam power transferred to the simulated alloy has effectively a median value slightly lower (by about 1 W) in the computations with predicted $A$ compared to the case with $A = 0.38$, which is as
Figure 14. Top view (left) and transverse cross section (right) comparisons between simulation results with locally predicted absorptance (variable $A$) and uniformly constant $A = 0.38$.

Figure 14. Top view (left) and transverse cross section (right) comparisons between simulation results with locally predicted absorptance (variable $A$) and uniformly constant $A = 0.38$.

expected. Moreover, with $A = 0.38$ the temporal variations of the laser power effectively absorbed are of $\pm 2$ W (at $U_{\text{laser}} = 5$ mm s$^{-1}$) to $\pm 3$ W at ($U_{\text{laser}} = 10$ mm s$^{-1}$) about the median value. With the variable $A$ these variations are slightly lower (at $U_{\text{laser}} = 5$ mm s$^{-1}$) or very similar ($U_{\text{laser}} = 10$ mm s$^{-1}$). Therefore, at quasi-steady state a slightly smaller amount of laser power effectively entered the workpiece in the computations with predicted $A$ compared to the simulation with $A = 0.38$. 
Figure 15. Absorptance at the melt pool free surface at different times before quasi-steady condition is reached. Case $U_{\text{las}} = 7.5 \text{ mm s}^{-1}$. The solid black line visualises the edge of the beam spot.

0.38, while it did result in slightly larger melt pool. Two possible reasons are identified. Consider figure 15 showing the plots of the absorptance field computed at the melt pool free surface at various times along the transient melt pool development. It can be seen that during the time interval $0.04 \text{ s} \leq t \leq 0.20 \text{ s}$ the maximum absorptance is larger than 0.38. It reaches 0.381 over a large part of the beam spot between 0.06 s and 0.10 s. It is believed that the time-variations of the absorptance along the transient melt pool development, with maximum value larger than during the subsequent quasi-steady state, could modify the thermocapillary flow and therefore to some extent the melt pool compared to the constant absorptivity coefficient $A = 0.38$. Figure 15 shows also important space-variations of the absorptance along the transient start period. Space variations of $A$ were also observed at quasi-steady state in figure 13. By definition, a model with constant absorptivity coefficient ignores these variations. Ebrahimi et al [13] did show that local variations in absorptivity amplify flow oscillations. It is therefore believed that the spatial non-uniformity of the absorptivity in the beam spot, not only during the transient
stage but also at quasi-steady state, could also affect the thermocapillary force and thus amplify flow oscillations compared to a uniform absorptivity coefficient. As known from [13], it could in turn enlarge the melt pool geometry.

6. Conclusions

In this study, a model for predicting the fraction of laser beam energy absorbed locally in the context of a thermo-fluid approach was proposed and applied to conduction mode laser metal fusion. This model accounts for local conditions at the processed surface that are its curvature and temperature. The modelling assumptions were highlighted and discussed. Among them, a first simplification consists in neglecting that, during the first milliseconds of the process (along which the beam interacts with an alloy still in solid state) inter-band absorption can take place when a 1 \( \mu \text{m} \) laser is used. A second is to assume no oxides at the beam metal interaction area. While the former simplification is believed to be of little consequence, the second is not unless a proper shielding gas or atmosphere control is used to protect the metal from oxidation. This model was applied for simulation of the melt pool in conduction mode welding of Ti-6Al-4V in argon atmosphere with a Yb fibre laser and different travel speeds. The results of this study show that

- Satisfactory agreement was obtained between the predicted absorptance and the few experimental measurements available for Ti-6Al-4V at the laser wavelengths of 10.6, 3.8 and 1.06 \( \mu \text{m} \).
- For 1.06 \( \mu \text{m} \) beam wavelength and Ti-6Al-4V the dependence of the absorptance is confirmed to be negligible in the range of incidence angle \( \theta_i \leq 30^\circ \).
- For 1.06 \( \mu \text{m} \) beam wavelength and Ti-6Al-4V the absorptance shows a clear temperature dependence over the range of incidence angle 0 \( \leq \theta_i \leq 80^\circ \).

The absorptivity coefficient of 0.34 that is often used in CFD simulation of conduction mode welding of Ti-6Al-4V with 1 \( \mu \text{m} \) wavelength laser beam underestimated the absorptance predicted in the studied test cases. It also led to a clear understimation of the melt pool dimensions.

- Good quantitative agreement was obtained between the CFD results with the locally predicted absorptance and the experimental measurements of the melt pool depth, width, length, free surface contour geometry, and the curvature of the end depression left after re-solidification at the laser stop location.
- The absorptance field predicted in the laser spot varies with:
  * processing conditions,
  * time during the transient melt pool development,
  * space along the transient melt pool development, and
to some extent at quasi-steady state, although the beam power density distribution was uniform (top-hat).

Finally, it is anticipated that a non-uniform laser beam power density distribution due to beam defocussing or beam shaping, or a metal alloy with stiffer variations of electric conductivity with temperature than Ti-6Al-4V, would lead to a wider spread of the absorptance distribution in the beam spot.

Data availability statement

The data that support the findings of this study are available upon reasonable request from the authors.

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ORCID IDs

Seyyed Mohammad Ali Noori Rahim Abadi  © https://orcid.org/0000-0002-6102-9021
Yongcui Mi © https://orcid.org/0000-0002-8018-6145
Isabelle Choquet © https://orcid.org/0000-0003-2535-8132

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