The Effect of Groove Shape on Molten Metal Flow Behaviour in Gas Metal Arc Welding

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Abstract

One of the challenges for development, qualification and optimisation of arc welding processes lies in characterising the complex melt-pool behaviour which exhibits highly non-linear responses to variations of process parameters. The present work presents a simulation-based approach to describe the melt-pool behaviour in root-pass gas metal arc welding (GMAW). Three-dimensional numerical simulations have been performed using an enhanced physics-based computational model to unravel the effect of groove shape on complex unsteady heat and fluid flow in GMAW. The influence of surface deformations on power-density distribution and the forces applied to the molten material were taken into account. Utilising this model, the complex heat and fluid flow in melt pools was visualised and described for different groove shapes. Additionally, experiments were performed to validate the numerical predictions and the robustness of the present computational model is demonstrated. The model can be used to explore physical effects of governing fluid flow and melt-pool stability during gas metal arc root welding.

Keywords: Gas metal arc welding (GMAW), Melt-pool behaviour, Joint shape design, Computational modelling
1 Introduction

Gas metal arc welding (GMAW) is a fusion-based joining technique that is widely employed in industry to join metallic parts and to produce high-integrity structures. The quality of the joints made using arc welding or the structures made using wire-arc additive manufacturing depend on chosen process parameters, material properties and boundary conditions \[1,3\]. Changes in operating variables can alter the magnitude and distribution of the heat input and forces applied to the molten metal in melt pools (such as Marangoni, Lorentz, thermal buoyancy forces and arc plasma shear stresses and pressures), affecting fluid flow in the pool and in turn the properties, structure and quality of products \[2\]. Correct control of melt-pool behaviour during arc welding is crucial to produce joints with desired properties \[4\].

One of the challenges for development, qualification and optimisation of arc welding processes lies in characterising the complex melt-pool behaviour which exhibits highly non-linear responses to variations of process parameters \[5\]. Trial-and-error experiments are often employed to realise appropriate processing parameters to achieve the desired properties. Such an experimental approach is costly and time inefficient and a successful processing for a specific configuration (e.g. material system, welding machine and joint shape) might not apply to a different configuration. Moreover, experimental identification of the effects of various parameters on the melt-pool behaviour is generally complicated due to the high-temperature, rapid solid-liquid phase transformation, opacity and fast dynamics of the molten metal flow \[4\]. Simulation-based approaches offer understanding of the melt-pool behaviour during welding and additive manufacturing and can serve as an alternative to experiments to explore the design space for process optimisation \[1,6\].

To date, focus has predominantly been placed on developing numerical simulations to describe melt-pool behaviour in arc welding of flat plates without a groove (i.e. bead-on-plate welding, see for instance, \[7\]-\[15\]); however little attention has been paid to understanding the effect of joint shape on complex heat and molten metal flow. Zhang et al. \[16,17\] developed a three-dimensional model in a body-fitted coordinate system to describe the effects of various driving forces on heat and fluid flow in the melt pool during GMAW fillet welding. Hu and Tsai \[18\] developed a comprehensive model to simulate unsteady molten metal flow and heat transfer in melt-pools during GMA welding of a thick plate with V-groove. These studies only focus on partially penetrated pools and do not report the effect of different joint shapes on molten metal flow behaviour. Chen et al. \[19\] studied the effect of the opening angle of a V-groove on melt-pool behaviour during relatively high-current GMAW (welding current \(I = 340\) A) using a computational model developed on the basis of a body-fitted coordinate system. They reported that changes in the opening angle have an insignificant effect on the flow pattern in the pool but can affect the velocity and temperature distribution and thus the pool shape. Using the Abel inversion method, Cho and Na \[20\] reconstructed the emissivity distribution of an arc plasma and argued that the application of V-grooves in arc welding can affect the arc plasma characteristics, changing the distribution of the power-density, arc pressure and
electromagnetic forces \cite{21}. On the basis of their previous studies \cite{20,21}, Cho et al. \cite{22} employed an elliptically symmetric distribution functions for power-density and arc pressure (instead of an axisymmetric distribution) to simulate heat and fluid flow in GMAW of a plate with V-groove at different welding positions. Changes in the groove shape due to filler metal deposition and its effect on the distribution of power-density and arc-induced forces were not accounted for in previous models that are available in the literature. Further investigations are required to realise the influence of the joint shape on molten metal flow behaviour in GMAW, particularly for fully-penetrated melt pools.

Focusing on understanding the melt-pool behaviour during root-pass gas metal arc welding, with particular interest in the effects of groove shape, a systematic numerical study was carried out in the present work. Three-dimensional calculations have been performed using a physics-based computational model to simulate the dynamics of heat and molten metal flow in GMAW. Additionally, experiments were performed to validate the numerical predictions. The present work explains the dynamics of internal molten metal flow in gas metal arc welding and provides an enhanced computational model for design space explorations.

## 2 Problem description

In gas metal arc welding, an electric arc between a consumable electrode (filler metal) and a workpiece provides the thermal energy required for melting the material. Melting of the filler metal results in the periodic formation of molten metal droplets that successively impinge on the workpiece surface. Thermal energy input from the arc plasma as well as the thermal energy transported by the droplets leads to the formation of a melt pool that creates a joint after solidification (see figure 1). In the present work, the effect of the groove shape on molten metal flow behaviour is studied for three different groove shapes, as shown schematically in figure 1. A torch, which is perpendicular to the workpiece top-surface is adopted here and the contact-tip to workpiece distance (CTWD) is set to 18 mm. Different values of welding current ranging between 220 A and 280 A have been studied. Details of the welding parameters in the present work are listed in table 1. The process parameters employed in the present work have been chosen based on preliminary trial experiments and are also comparable to those reported in previous independent studies on gas metal arc welding of steel plates with grooves (see for instance, \cite{17,18,21,22}). The plates are made of a stainless steel alloy (AISI 316L) and are initially at an ambient temperature of 300 K. The welding torch is initially located in the middle of the workpiece along the x-axis and 10 mm away from the leading-edge of the workpiece (i.e. y = 10 mm).

The computational domain is defined in a stationary Cartesian coordinate system and is in the form of a rectangular cube that encompasses the metallic workpiece and two layers of gas below and above the workpiece. The incorporation of the gas layers allows tracking of surface deformations of the pool. To reduce the complexity of simulations and computation time, the melt-pool is
Table 1: Welding parameters studied in the present work.

| Parameter                        | Value   | Unit     |
|----------------------------------|---------|----------|
| Welding current $I$              | 220 − 280 | A        |
| Arc voltage $U$                  | 21.4 − 23.0 | V       |
| Wire feed rate $u_w$             | 7.0 − 8.7 | m min$^{-1}$ |
| Wire diameter $d_w$              | 1.2 (0.045) | mm (inch) |
| Wire material AISI 316L          | –       | –        |
| Travel speed $V$                 | 7.5     | mm s$^{-1}$ |
| Shielding gas                    | 97.5% Ar + 2.5% CO$_2$ | –        |
| Shielding gas flow rate          | 20      | 1 min$^{-1}$ |
| Inner diameter of the shielding cup | 20  | mm        |
| CTWD                             | 18      | mm       |
| Distance between the contact tip and the shielding cup edge | 2 | mm |
| Torch angle                      | 90      | °        |

decoupled from the arc plasma in the simulations. Accordingly, the heat input from the arc and the arc induced forces are defined as source terms for thermal energy and momentum. These source terms are adjusted dynamically during the calculations, as explained in section 3, to account for the changes in the arc power and power-density distribution as well as the magnitude and distribution of the forces exerted by the arc plasma that occur due to melt-pool surface deformations and filler metal deposition. The conditions applied to the outer boundaries of the computational
domain are shown in figure 1. The outer boundaries of the plates are treated as no-slip walls, as the melt-pool does not reach them, and heat losses due to radiation and convection are accounted for. A fixed atmospheric pressure (101.325 Pa) is applied to the outer boundaries of the gas layers. The thermophysical properties of AISI 316L and the gas employed in the simulations are presented in table 2 and figure 2. The values for the surface tension are estimated using an empirical correlation proposed by Sahoo et al. [23], which takes the influence of surfactants (i.e. sulphur) into account. Employing a temperature-dependent density model, thermal buoyancy force are accounted for in the simulations. In the present work, the properties of the shielding gas are assumed to be temperature-independent, which is a common assumption in numerical simulations of arc welding and additive manufacturing where the melt-pool is decoupled from the arc plasma [7–9, 11–13, 22]. This assumption is justifiable as the transport properties of the shielding gas (i.e. viscosity, density and thermal conductivity) are small compared to those of the molten metal, and thus changes in the shielding gas properties with temperature negligibly affect the numerical predictions of fluid flow in the melt pool [24].

Table 2: Thermophysical properties of the stainless steel (AISI 316L) and the gas employed in the numerical simulations. Values for AISI 316 are taken from [25].

| Property                        | Stainless steel (AISI 316) | Gas             | Unit          |
|---------------------------------|----------------------------|-----------------|---------------|
| Density $\rho$                  | see figure 2               | 1.623           | kg m$^{-3}$   |
| Specific heat capacity $c_p$    | see figure 2               | 520.64          | J kg$^{-1}$ K$^{-1}$ |
| Thermal conductivity $k$        | see figure 2               | $1.58 \times 10^{-2}$ | W m$^{-1}$ K$^{-1}$ |
| Viscosity $\mu$                 | see figure 2               | $2.12 \times 10^{-5}$ | kg m$^{-1}$ s$^{-1}$ |
| Latent heat of fusion $L$       | $2.7 \times 10^5$         | –               | J kg$^{-1}$   |
| Liquidus temperature $T_l$      | 1723                       | –               | K             |
| Solidus temperature $T_s$       | 1658                       | –               | K             |
Figure 2: Temperature-dependent thermophysical properties of AISI 316L employed in the simulations. (a) density \[26\], (b) specific heat capacity at constant pressure \[25\], (c) thermal conductivity \[25\], (d) dynamic viscosity \[26\] and (e) surface tension \[23\].

3 Methods

3.1 Mathematical model

A three-dimensional multiphase model has been developed to predict molten metal flow, heat transfer and associated surface movements in gas metal arc welding. In the present model, the fluids (i.e. the molten metal and the gas) are considered to be Newtonian and their densities are assumed to pressure-independent. Assuming that the fluid flows under consideration are in the continuum regime, the dynamics of heat and fluid flow in melt pools and their surroundings are governed by the equations of motion given by the conservation equations for mass, momentum and energy.
Accordingly, the unsteady governing equations are cast as follow:

\[
\begin{align*}
\frac{D\rho}{Dt} &= S_m, \\
\rho \frac{Du}{Dt} &= \mu \nabla^2 u - \nabla p + F_d + F_s + F_b + S_m (u_s - u), \\
\rho \frac{Dh}{Dt} &= \frac{k}{c_p} \nabla^2 h - \rho \frac{D(\psi L_f)}{Dt} + S_q + S_l + S_m \left( \frac{L_f}{T_1} c_p dT \right),
\end{align*}
\]

where, \(\rho\) is the density, \(u\) the relative fluid-velocity vector, \(u_s\) the fluid-velocity vector for the filler metal droplet, \(t\) the time, \(\mu\) the dynamic viscosity, \(p\) the pressure, \(h\) the sensible heat, \(k\) the thermal conductivity, \(c_p\) the specific heat capacity at constant pressure, \(\psi L_f\) the latent heat, and \(S_m\) the source term defined to model filler metal addition \[27\]. The subscripts ‘d’ and ‘i’ indicate the droplet and initial condition respectively. The total enthalpy of the material \(\mathcal{H}\) is the sum of the latent heat \((\psi L_f)\) and the sensible heat \(h\) and is defined as follows \[28\]:

\[
\mathcal{H} = \left( h_r + \int_{T_i}^{T} c_p dT \right) + \psi L_f,
\]

where, \(T\) is the temperature, \(\psi\) the local liquid volume-fraction, and \(L_f\) the latent heat of fusion. The subscript ‘r’ indicates the reference condition. Assuming the liquid volume-fraction \(\psi\) to be a linear function of temperature \[28\], its value can be calculated as follows:

\[
\psi = \frac{T - T_s}{T_l - T_s}; \quad T_s \leq T \leq T_l,
\]

where, \(T_l\) and \(T_s\) are the liquidus and solidus temperatures, respectively.

To capture the position of the gas-metal interface, the volume-of-fluid (VOF) method \[29\] is adopted, where the scalar function \(\phi\) indicates the local volume-fraction of a phase in a given computational cell. The value of \(\phi\) varies from 0 in the gas phase to 1 in the metal phase, and cells with \(0 < \phi < 1\) represent the gas-metal interface. The linear advection equation describes the advection of the scalar function \(\phi\) as follows:

\[
\frac{D\phi}{Dt} = \frac{S_m}{\rho}.
\]

Accordingly, the effective thermophysical properties of the material in each computational cell are determined as follows:

\[
\xi = \phi \xi_m + (1 - \phi) \xi_g,
\]

where, \(\xi\) corresponds to thermal conductivity \(k\), specific heat capacity \(c_p\), viscosity \(\mu\) or density \(\rho\),
and subscripts ‘g’ and ‘m’ indicate gas or metal respectively.

Solid–liquid phase transformation occurs in the temperature range between \( T_s \) and \( T_l \) in the so-called ‘mushy zone’. To model the damping of liquid velocities in the mushy zone, and suppression of liquid velocities in solid regions, the sink term \( F_d \) based on the enthalpy-porosity technique [30], is incorporated into the momentum equation and is defined as

\[
F_d = -C \left( \frac{1 - \psi}{\psi^3 + \epsilon} \right)^2 \psi^3 + \epsilon \mathbf{u},
\]  

(8)

where, \( C \) is the mushy-zone constant and \( \epsilon \) is a constant, equal to \( 10^{-3} \), employed to avoid division by zero. Depending on the melting temperature range as well as the imposed boundary conditions, the value of the mushy-zone constant can affect the numerical predictions of solidification and melting simulations. The value of the mushy-zone constant should be assigned appropriately to avoid numerical artefacts in simulations of solid–liquid phase transformations, which is discussed in detail in [31]. In the present work, the value of the mushy-zone constant \( C \) was chosen to equal \( 10^7 \) kg m\(^{-2}\) s\(^{-2}\) [31].

To model forces acting on the gas-metal interface such as surface tension, thermocapillary and arc plasma forces, the continuum surface force (CSF) model [32] is employed. In the CSF model, surface forces are considered as volumetric forces acting on the material contained in grid cells in the interface region. The source term \( F_s \) is included in equation (2) as follows:

\[
F_s = f_s \| \nabla \phi \| \frac{2\rho}{\rho_m + \rho_g},
\]  

(9)

where, subscripts ‘g’ and ‘m’ indicate gas or metal respectively. In equation (9), \( f_s \) is the surface force applied to a unit area, and the term \( 2\rho/(\rho_m + \rho_g) \) is employed to abate the effect of the large metal-to-gas density ratio by redistributing the volumetric surface-forces towards the metal phase \( (i.e. \text{the heavier phase}) \). In addition to surface forces, body forces \( (i.e. \text{electromagnetic forces}) \) are incorporated in the source term \( F_b \) in equation (2).

To model the thermal energy input to the material, the source \( S_q \) is included in equation (3). Moreover, heat losses from the workpiece surface due to convection and radiation are accounted for by including the sink term \( S_l \) in equation (3).

In gas metal arc welding, the surface force acting on the gas-metal interface \( f_s \) includes an arc plasma term, surface tension and thermocapillary forces, and is defined as follows:

\[
f_s = f_a + \gamma \kappa \hat{n} + \frac{d\gamma}{dT} \left[ \nabla T - \hat{n} \left( \hat{n} \cdot \nabla T \right) \right],
\]  

(10)

where, \( f_a \) is arc plasma force, \( \gamma \) the surface tension, \( \hat{n} \) the surface unit normal vector \( (\hat{n} = \nabla \phi/\| \nabla \phi \|) \) and \( \kappa \) the surface curvature \( (\kappa = \nabla \cdot \hat{n}) \). The arc plasma force \( f_a \) defined in equation (10) comprises arc plasma shear stress \( f_r \) and arc pressure \( f_p \),
\[ f_a = f_r + f_p. \] (11)

The arc plasma shear stress \( f_r \), which acts at a tangent to the surface, is defined as follows [33]:

\[ f_r = \left[ \tau_{\text{max}} g_r (R, \sigma_r) \right] \hat{t}, \] (12)

where, the maximum arc shear stress \( \tau_{\text{max}} \) [34,35], the arc shear stress distribution function \( g_r \) [36] and the surface unit tangent vector \( \hat{t} \) [33] were defined as follows:

\[ \tau_{\text{max}} = 7 \times 10^{-2} I^{1.5} \exp \left( \frac{-2.5 \times 10^4 \bar{\ell}}{I^{0.985}} \right), \] (13)

\[ g_r (R, \sigma_r) = \sqrt{\frac{R}{\sigma_r}} \exp \left( \frac{-R^2}{\sigma_r^2} \right), \] (14)

\[ \hat{t} = \frac{r - \hat{n} (\hat{n} \cdot r)}{\| r - \hat{n} (\hat{n} \cdot r) \|}. \] (15)

Here, \( I \) is the welding current in Amperes, \( \bar{\ell} \) the mean arc length in meters, \( R \) the radius in \( x-y \) plane (i.e. \( R = \sqrt{x^2 + y^2} \)) in meters, and \( r \) the position vector in the \( x-y \) plane in meters. The distribution parameter \( \sigma_r \) (in meters) is assumed to be a function of the mean arc length \( \bar{\ell} \) and current \( I \) and was approximated on the basis of the data reported by Lee and Na [34]:

\[ \sigma_r = 1.387 \times 10^{-3} + I^{-0.595} \bar{\ell}^{0.733}. \] (16)

The arc pressure \( f_p \) is determined as follows [37]:

\[ f_p = F_p \left[ \frac{\mu_0 I}{4\pi} \frac{I}{2\pi} \frac{1}{\sigma_p} \exp \left( \frac{-R^2}{2\sigma_p^2} \right) \right] \hat{n}, \] (17)

where, \( I \) is the current in Ampere, and \( \mu_0 \) is the vacuum permeability equal to \( 4\pi \cdot 10^{-7} \text{H m}^{-1} \). The distribution parameter \( \sigma_p \) (in metres) was determined using the experimental data reported by Tsai and Eagar [38] as follows:

\[ \sigma_p = 7.03 \times 10^{-2} \bar{\ell}^{0.823} + 2.04 \times 10^{-4} I^{0.376}, \] (18)

where, \( \ell \) is the local arc length in meters, and \( I \) the current in Amperes. Changes in surface morphology can cause the total arc force applied to the melt-pool surface \( \int \int \| f_p \| dV \) to differ from the expected arc force \( \mu_0 I^2 / 4\pi \) due to changes in \( \| \nabla \phi \| \) [39,40]. This numerical artefact is negated by incorporating \( F_p \), defined as follows:
The dimensionless factor $j$ is employed, as suggested by Lin and Eagar \cite{37} and Liu et al. \cite{41}, to match the theoretically determined arc pressure with experimentally measured values, and is calculated as follows:

$$j = 3 + 8 \times 10^{-3} I,$$

with $I$ the welding current in Amperes.

$F_b$ in equation (2) is the body force, which comprises electromagnetic and gravity forces. The electromagnetic force was computed using the model proposed by Tsao and Wu \cite{42} transformed into a body-fitted coordinate system. Hence, the body forces are defined as follows:

$$f_{b_x} = -\frac{\mu_0 I^2}{4\pi^2\sigma_e^2 R} \exp \left( -\frac{R^2}{2\sigma_e^2} \right) \left[ 1 - \exp \left( -\frac{R^2}{2\sigma_e^2} \right) \right] \left( 1 - \frac{z - z'}{H_m - z'} \right)^2 \left( \frac{x}{R} \right),$$

$$f_{b_y} = -\frac{\mu_0 I^2}{4\pi^2\sigma_e^2 R} \exp \left( -\frac{R^2}{2\sigma_e^2} \right) \left[ 1 - \exp \left( -\frac{R^2}{2\sigma_e^2} \right) \right] \left( 1 - \frac{z - z'}{H_m - z'} \right)^2 \left( \frac{y}{R} \right),$$

$$f_{b_z} = -\frac{\mu_0 I^2}{4\pi^2 R^2 H_m} \left[ 1 - \exp \left( -\frac{R^2}{2\sigma_e^2} \right) \right] \left( 1 - \frac{z - z'}{H_m - z'} \right) + \rho g.$$

Here, the distribution parameter for the electromagnetic force $\sigma_e$ is the same as $\sigma_p$, according to Tsai and Eagar \cite{38}, $z'$ is the position of the melt-pool surface in $x$-$y$ plane at a given time $t$, and $g$ the gravitational acceleration vector. It should be noted that the current-density profile is assumed to be Gaussian in the model proposed by Tsao and Wu \cite{42} to compute the electromagnetic forces. Further studies are required to develop a generic model to approximate the evolution of current-density profile during gas metal arc welding \cite{43,44}.

The thermal energy provided by the arc is modelled by adding the source term $S_q$ to the energy equation (equation (3)) and was defined as

$$S_q = \mathcal{F}_q \left[ \frac{\eta_p U}{2\pi \sigma_q^2} \exp \left( -\frac{R^2}{2\sigma_q^2} \right) \| \nabla \phi \| \frac{2 \rho_c p}{(\rho_c p)_m + (\rho_c p)_g} \right],$$

where, the arc efficiency $\eta_a$ is defined as follows:

$$\eta_a = \eta_p - \eta_d.$$

Here, $\eta_p$ is the process efficiency and is assumed to vary linearly with welding current from 77\% at 200 A to 72\% at 300 A \cite{46}, and $\eta_d$ is the efficiency of thermal energy transfer by molten metal.
droplets, which is defined as follows:

$$\eta_d = \frac{q_d}{T U},$$  \hspace{1cm} (26)$$

with $q_d$ the thermal energy content of the droplets that are assumed to be spherical. $q_d$ is defined as follows:

$$q_d = \rho_d \frac{4}{3} \pi r_d^3 \left( L_f + \int_{T_i}^{T_d} c_p dT \right) f_d,$$  \hspace{1cm} (27)$$

where, $r_d$ is the radius of molten metal droplet. The droplet temperature $T_d$ was approximated to 2500 K, based on the experimental data reported by Soderstrom et al. [47]. $f_d$ in equation (27) is the frequency of droplet detachment, and is defined as:

$$f_d = \frac{3 u_w r_w^2}{4 r_d^3},$$  \hspace{1cm} (28)$$

where, $u_w$ is the wire feed rate and $r_w$ is the radius of the welding wire. For metal transfer in the spray mode, the radius of the molten metal droplets and the welding wire are assumed to be the same. Accordingly, the magnitude of molten metal droplet velocity $u_d$ just after detachment was approximated using the correlation proposed by Lin et al. [48]:

$$u_d = \frac{I^2}{2 \pi r_d} \sqrt{\frac{3 \mu_0 \rho_d}{\mu_0}} G,$$  \hspace{1cm} (29)$$

where, $I$ is in Ampere, $r_d$ the radius of the droplet in meters, $\mu_0$ the vacuum permeability in H m$^{-1}$, $\rho_d$ the density of the molten droplet in kg m$^{-3}$, and $G$ a dimensionless constant introduced to obtain agreement with experimental measurements equal to 0.98 for steel electrodes.

The process voltage $U$ was assumed to be a function of welding current and arc length [49-51], and was determined as follows:

$$U = U_w + U_o + U_a.$$  \hspace{1cm} (30)$$

Here, $U_w$ is the wire voltage assumed to be constant and equal to 7 V [50], $U_o$ the sum of the electrode fall voltages is assumed to be a function of welding current $I$:  

$$U_o = C_I I + 10,$$  \hspace{1cm} (31)$$

with $I$ in Ampere and $C_I$ the coefficient of variation of the electric fall voltage with current equal to 0.016 V A$^{-1}$ [50]. $U_a$ in equation (30) is the arc column voltage:

$$U_a = C_e \ell,$$  \hspace{1cm} (32)$$
with $\ell$ in meters and $C_e$ the electric field strength equal to 1.09 V mm$^{-1}$ \cite{50, 51}. Using the data reported by Tsai and Eagar \cite{38}, the distribution parameter $\sigma_q$ (in meters) was determined as follows:

$$
\sigma_q = 1.61 \times 10^{-1} \ell^{0.976} + 2.23 \times 10^{-4} I^{0.395},
$$

(33)

with $\ell$ in meters and $I$ in Ampere. The adjustment factor $\mathcal{F}_q$ was used to negate changes in the total heat input due to surface deformations \cite{52, 53}, which is defined as follows:

$$
\mathcal{F}_q = \frac{\eta I U}{\llap{\sum} S_q dV}.
$$

(34)

It should be noted that the source term $S_q$ is only applied to the top surface of the workpiece.

The sink term $S_l$ was added to the energy equation to account for heat losses due to convection and radiation, and is determined as follows:

$$
S_l = - \left[ h_c (T - T_0) + \mathcal{K}_b \varepsilon (T^4 - T_0^4) \right] \| \nabla \phi \| \frac{2 \rho c_p}{(\rho c_p)_m + (\rho c_p)_g},
$$

(35)

where, $h_c$ is the heat transfer coefficient equal to 25 W m$^{-2}$ K$^{-1}$ \cite{54}, $\mathcal{K}_b$ the Stefan–Boltzmann constant, and $\varepsilon$ the radiation emissivity equal to 0.45 \cite{55}.

### 3.2 Numerical implementation

The computational model employed in the present work was developed within the framework of a proprietary finite-volume solver, ANSYS Fluent \cite{56}. To implement the source terms in the governing equations and the surface tension model, user-defined subroutines programmed in the C programming language were used. The computational domain contains about $2.7 \times 10^6$ non-uniform hexahedral cells, with the smallest cell spacing being set to 80 $\mu$m in the melt-pool region, which is sufficiently fine to obtain grid-independent solutions \cite{39, 40, 52, 53}. The cell spacing increases gradually from the melt-pool region towards the boundaries of the computational domain and the maximum cell size was limited to 400 $\mu$m. The central-differencing scheme with second-order accuracy was employed for spatial discretisation of momentum advection and diffusive fluxes. A first-order implicit scheme was employed for the time marching, and a fixed time-step size of $2 \times 10^{-5}$ s was used to keep the value of the Courant number ($Co = \| u \| \Delta t / \Delta x$) below 0.25. To formulate the advection of the volume-fraction scalar field, an explicit compressive VOF method \cite{57} was employed. Moreover, the PRESTO (pressure staggering option) scheme \cite{58} and the PISO (pressure-implicit with splitting of operators) scheme \cite{59} was employed for the pressure interpolation and coupling velocity and pressure fields, respectively. Simulations were executed in parallel on a high-performance computing cluster, each on 70 cores (AMD EPYC 7452) and the total run-time was about 290 h.
3.3 Experimental setup and procedure

The general process parameters studied in the present work are introduced in section 2. Figure 3 shows a schematic drawing of the experimental setup utilised in the present work. A Fronius CMT 5000i power source that was attached to a six-axis Fanuc robot was employed. Weld beads with a length of 80 mm were deposited on the workpiece with pre-machined grooves. Each experiment was repeated at least three times to ensure repeatability of the tests. The filler metal and the workpiece employed in the experiments were AISI 316L. Welding current and voltage were measured and recorded at a frequency of 5 kHz during the experiments using a Triton 4000 data acquisition system. Samples were cut after the experiments to extract transverse cross-sections. The cut samples were mounted and surface ground using silicon carbide (SiC) papers with grit sizes varying from 80 to 2000 grit. Finally, the samples were polished using colloidal alumina with particle sizes of 3 µm and 1 µm respectively. Fusion zones were revealed by chemical etching with Kallings Reagent I (2 g CuCl₂ + 40 ml HCl + 40 ml C₂H₅OH + 40 ml H₂O) for 3 s. Macrographs of the fusion zones in the etched specimens were obtained using a Keyence digital microscope.

![Figure 3: Schematic of the experimental setup utilised in the present work.](image)

4 Results and discussion

4.1 Model validation

The reliability and accuracy of the present numerical predictions are benchmarked against experimentally measured melt-pool shapes. In this study, gas metal arc welding of workpieces with different groove shapes are considered, with a welding current of 280 A and a travel speed of 7.5 mm s⁻¹. Figure 4 shows a comparison between the numerically predicted melt-pool shapes with
those obtained from experiments for different groove shapes. The computational cells containing molten metal were marked during the calculation to visualise the melt-pool shapes. It is worth noting that the experiments were conducted after the numerical simulations, which means no calibration is performed to tune the numerical results. The results indicate a reasonable agreement between numerically predicted and experimentally measured melt-pool shapes. The maximum deviation between the predicted melt-pool dimensions and experimental measurements is found to be less than 10%, demonstrating the validity of the present numerical simulations. This deviation might be caused by uncertainties associated with the models employed to approximate the temperature-dependent material properties at elevated temperatures, the simplifying assumptions made to develop the computational model such as those employed to determine droplet size, velocity and temperature, and uncertainties in determining the boundary conditions in the model.

Figure 4: Comparison of the melt-pool shapes obtained from the present numerical simulations with experimental measurements for different groove shapes with a welding current of 280 A and a travel speed of 7.5 mm s$^{-1}$. Regions shaded in dark grey show the melt-pool shape obtained from numerical simulations. The computational cells that, at any stage during the transient calculations of the melting and re-solidification process, contained molten metal were marked to visualise the melt-pool shape on the transversal cross-section. Green symbols on experimental data show the melt-pool boundary obtained from numerical simulations. Yellow dashed-lines indicate the joint shape before welding.

### 4.2 Thermal and fluid flow fields

Once the arc ignites and the process begins, the welding wire heats up to the melting temperature and molten metal droplets form at the wire tip that detach periodically from the wire and deposit
on the workpiece surface as shown schematically in figure 1. The frequency of droplet detachment is directly proportional to the wire feed rate and ranges between 147 Hz and 187 Hz for the welding process parameters studied in the present work (see table 1). To simplify the numerical simulations and as described in section 3.1, the filler metal droplets are assumed to be spherical and are incorporated into the simulations with predefined velocity and temperature, which is a common practice in modelling melt-pool behaviour in gas metal arc welding (see for instance, [7,8,18,21]). The qualitative melt-pool behaviour was found to be similar for different welding currents studied in the present work. Therefore, representative results for the cases with welding current of 220 A are shown and discussed in the paper.

The thermal energy input from the plasma arc in addition to the thermal energy transported by the molten metal droplets result in the formation of a melt pool. For the process parameters studied in the present work (see table 1), the melt pool grows over time and reaches a quasi-steady-state condition after about 3 s. Figure 5 shows a partial view of the workpiece encompassing the melt pool and the corresponding thermal and fluid flow fields over the melt-pool surface for different groove shapes at \( t = 5 \) s with wire feed rate \( u_w = 7 \text{ m/min} \) and welding current \( I = 220 \) A. For the cases shown in figure 5, the maximum surface temperature is less than 2310 K and the value of the temperature gradient of surface tension (\( \partial \gamma / \partial T \)) is mostly positive (see figure 2(e)). Hence, the molten metal moves from the cold area close to the melt-pool rim towards the hot central region, primarily due to the Marangoni shear force induced over the surface. Molten metal streams from the melt-pool rim collide in the central region and form a complex unsteady asymmetric flow pattern in the pool. A similar flow pattern is observed experimentally in previous independent studies conducted by Wu et al. [60] and Zhao et al. [61]. The maximum local molten metal velocity is about \( 0.7 - 0.8 \text{ m/s} \) and corresponds to a Péclet number (\( \text{Pe} = \rho c_p \partial T / \mu / k \)) larger than unity (\( \mathcal{O}(400) \)), which signifies that advection dominates the energy transfer in the melt pool and that the process cannot be described adequately using a thermal model without considering fluid flow.

The results suggest that the energy transported to the surrounding solid material markedly affects the melt-pool shape. Although the total heat input to the material is the same for the cases shown in figure 5, the melt-pool shapes differ notably for different groove shapes. It appears that increasing the width of the root-leg results in a decrease in the amount of heat diffused to the side walls of the groove as the height of the deposit layer reduces, leading to an increase in the length of the melt-pool as well as the mushy-zone (i.e. regions between the solidus and liquid iso-surfaces in figure 5). Moreover, the average fluid velocity in the pool decreases with increasing width of the root-leg, which can be attributed to the decrease in the magnitude of temperature gradients generated over the surface. Among all the cases studied in the present work, full-penetration is observed only for those with root-leg, even for the case with welding current \( I = 280 \) A. Evidently, a higher welding current or a lower travel speed is required to achieve full penetration using grooves without root-leg (i.e. V-groove). However, increasing the welding current or reducing the travel
Figure 5: The numerically predicted thermal and fluid fields over the melt-pool surface (left column) and the corresponding pool shape (right column) for different joint shapes at $t = 5$ s. (a) groove angle $\theta = 60^\circ$ and no root-leg ($W_r = 0$ mm), (b) $\theta = 20^\circ$ and root-leg width $W_r = 4$ mm and (c) $\theta = 20^\circ$ and root-leg width $W_r = 6$ mm. Wire feed rate $u_w = 7$ m min$^{-1}$, welding current $I = 220$ A, and travel speed $V = 7.5$ m s$^{-1}$. The area between iso-surfaces of solidus and liquidus temperature shows the mushy region.

speed results in an increase in total heat input to the material, which is often undesirable as it decreases the cooling rate and can adversely affect the properties of the joint, particularly when austenitic stainless steels are used [62–64]. Moreover, increasing the welding current can lead to a significant increase in arc force as the arc force is proportional to the welding current squared ($\|F_{\text{arc}}\| \propto I^2$) [49], and thus limiting the welding current tolerance to avoid defects such as burn-through. Despite the fact that employing a root-leg can reduce the welding current required to achieve full penetration, employing a relatively wide root-leg may increase the number of welding
passes required to fill the groove.

Figure 6 shows thermal and fluid flow fields in the $x = 0$ plane for different joint shapes and time instances. The impingement of molten metal droplets on the surface disturbs the thermal and fluid flow field in the pool and results in the formation of a crater and a travelling wave over the melt-pool surface, as indicated by arrows in figure 6. Moreover, the periodic molten metal droplet impingement on the melt pool enhances mixing in the melt pool. The molten metal droplet temperature ($T_d = 2500 \text{K}$) is above the critical temperature at which the sign of surface-tension temperature coefficient ($\partial \gamma / \partial T$) changes from positive to negative ($T_{cr} \approx 2250 \text{K}$); therefore, an outward fluid flow is induced on the surface in the region where the droplet is impinged due to Marangoni shear force. Soon after the droplet is merged with the melt pool, the crater closes due to surface tension and hydrostatic forces, and the surface temperature decreases to values less than $2310 \text{K}$ for which the value of $\partial \gamma / \partial T$ is mostly positive. The wave crests move radially outward towards the melt-pool rim and are reflected by the solid edges of the pool. Interactions between the primary and reflected waves as well as the forces acting on the molten material result in complex melt-pool surface deformations and oscillations, as shown in figure 5. For the cases studied in the present work, the frequency of the droplet transfer in relatively high ($\mathcal{O}(170 \text{Hz})$) and the droplet sizes are relatively small compared to the melt-pool dimension, resulting in a smooth weld bead with negligible ripple formation.
Figure 6: Melt-pool shape, temperature profile and velocity vectors in the $x = 0$ plane for different joint shapes and time instances. (left column) groove angle $\theta = 60^\circ$ and no root-leg ($W_r = 0$ mm), (middle column) $\theta = 20^\circ$ and root-leg width $W_r = 4$ mm and (right column) $\theta = 20^\circ$ and root-leg width $W_r = 6$ mm. Wire feed rate $u_w = 7$ m min$^{-1}$, welding current $I = 220$ A, and travel speed $V = 7.5$ m s$^{-1}$.

5 Conclusion

Three-dimensional numerical simulations were performed to systematically investigate the effect of groove shape on melt-pool behaviour in root pass gas metal arc welding (GMAW). The effects of
melt-pool surface deformations on power-density distribution and the forces applied to the molten material were accounted for in the present computational model. These effects are often neglected in numerical simulations of melt-pool behaviour in arc welding. Thermal and fluid flow fields in the melt pool are visualised and described for different groove shapes. Moreover, experiments were conducted to validate the numerical predictions.

Energy transfer in melt pools during gas metal arc welding is dominated by convection and thus thermal models without considering fluid flow cannot predict and describe the melt-pool shape with sufficient accuracy. The periodic impingement of molten metal droplets disturbs the thermal and fluid flow fields in the pool, resulting in an even more complex flow pattern. For the process parameters studied in the present work, full-penetration was observed only for the grooves with root-leg. Changes in the groove shape have an insignificant influence on the flow pattern over the surface, however the groove shape affects the energy transfer to the surrounding solid material and thus alters the melt-pool shape and can affect the properties of the joint. The groove shape also affects the melt-pool oscillatory behaviour as it influences the reflection of the waves generated due to the molten metal droplet impingement. Moreover, the groove shape can affect the process window, which can be explored using the simulation-based approach described in the present work.

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**Author Contributions**

Conceptualisation, A.E. and I.M.R.; methodology, A.E.; software, A.E.; validation, A.E.; formal analysis, A.E.; investigation, A.E.; resources, A.E. and M.J.M.H.; data curation, A.E. and A.B.; writing—original draft preparation, A.E.; writing—review and editing, A.E., A.B., C.R.K., M.J.M.H. and I.M.R; visualisation, A.E.; supervision, C.R.K. and I.M.R.; project administration, A.E.; and funding acquisition, I.M.R. and M.J.M.H.

**Conflict of interest**

The authors declare no conflict of interest.
Data availability

The data generated in this study are available on reasonable request from the corresponding author.

References

[1] H. L. Wei, T. Mukherjee, W. Zhang, J. S. Zuback, G. L. Knapp, A. De, T. DebRoy, Mechanistic models for additive manufacturing of metallic components, Progress in Materials Science 116 (2021) 100703. doi:10.1016/j.pmatsci.2020.100703

[2] J. Norrish, J. Polden, I. Richardson, A review of wire arc additive manufacturing: development, principles, process physics, implementation and current status, Journal of Physics D: Applied Physics 54 (47) (2021) 473001. doi:10.1088/1361-6463/ac1e4a

[3] T. DebRoy, S. A. David, Physical processes in fusion welding, Reviews of Modern Physics 67 (1) (1995) 85–112. doi:10.1103/revmodphys.67.85

[4] L. Aucott, H. Dong, W. Mirihanage, R. Atwood, A. Kidess, S. Gao, S. Wen, J. Marsden, S. Feng, M. Tong, T. Connolley, M. Drakopoulos, C. R. Kleijn, I. M. Richardson, D. J. Browne, R. H. Mathiesen, H. V. Atkinson, Revealing internal flow behaviour in arc welding and additive manufacturing of metals, Nature Communications 9 (1). doi:10.1038/s41467-018-07900-9

[5] S. A. David, T. DebRoy, Current issues and problems in welding science, Science 257 (5069) (1992) 497–502. doi:10.1126/science.257.5069.497

[6] P. S. Cook, A. B. Murphy, Simulation of melt pool behaviour during additive manufacturing: Underlying physics and progress, Additive Manufacturing 31 (2020) 100909. doi:10.1016/j.addma.2019.100909

[7] R. Zong, J. Chen, C. Wu, D. Lou, Numerical analysis of molten metal behavior and undercut formation in high-speed GMAW, Journal of Materials Processing Technology 297 (2021) 117266. doi:10.1016/j.jmatprotec.2021.117266

[8] Z. Hu, L. Hua, X. Qin, M. Ni, F. Ji, M. Wu, Molten pool behaviors and forming appearance of robotic GMAW on complex surface with various welding positions, Journal of Manufacturing Processes 64 (2021) 1359–1376. doi:10.1016/j.jmapro.2021.02.061

[9] H. H. Zargari, K. Ito, M. Kumar, A. Sharma, Visualizing the vibration effect on the tandem-pulsed gas metal arc welding in the presence of surface tension active elements, International Journal of Heat and Mass Transfer 161 (2020) 120310. doi:10.1016/j.ijheatmasstransfer.2020.120310

[10] D. Wu, S. Tashiro, Z. Wu, K. Nomura, X. Hua, M. Tanaka, Analysis of heat transfer and material flow in hybrid KPAW-GMAW process based on the novel three dimensional CFD simulation, International Journal of Heat and Mass Transfer 147 (2020) 118921. doi:10.1016/j.ijheatmasstransfer.2019.118921

[11] D. Wu, X. Hua, D. Ye, F. Li, Understanding of humping formation and suppression mechanisms using the numerical simulation, International Journal of Heat and Mass Transfer 104 (2017) 634–643. doi:10.1016/j.ijheatmasstransfer.2016.08.110
[12] J. Hu, H. Guo, H. L. Tsai, Weld pool dynamics and the formation of ripples in 3D gas metal arc welding, International Journal of Heat and Mass Transfer 51 (9-10) (2008) 2537–2552. doi:10.1016/j.ijheatmasstransfer.2007.07.042

[13] M. H. Cho, D. F. Farson, Understanding bead hump formation in gas metal arc welding using a numerical simulation, Metallurgical and Materials Transactions B 38 (2) (2007) 305–319. doi:10.1007/s11663-007-9034-5

[14] M. Ushio, C. S. Wu, Mathematical modeling of three-dimensional heat and fluid flow in a moving gas metal arc weld pool, Metallurgical and Materials Transactions B 28 (3) (1997) 509–516. doi:10.1007/s11663-997-0118-z

[15] J.-W. Kim, S.-J. Na, A study on the three-dimensional analysis of heat and fluid flow in gas metal arc welding using boundary-fitted coordinates, Journal of Engineering for Industry 116 (1) (1994) 78–85. doi:10.1115/1.2901812

[16] W. Zhang, C.-H. Kim, T. DebRoy, Heat and fluid flow in complex joints during gas metal arc welding—Part I: Numerical model of fillet welding, Journal of Applied Physics 95 (9) (2004) 5210–5219. doi:10.1063/1.1699485

[17] W. Zhang, C.-H. Kim, T. DebRoy, Heat and fluid flow in complex joints during gas metal arc welding—part II: Application to fillet welding of mild steel, Journal of Applied Physics 95 (9) (2004) 5220–5229. doi:10.1063/1.1699486

[18] J. Hu, H. L. Tsai, Modelling of transport phenomena in 3D GMAW of thick metals with V groove, Journal of Physics D: Applied Physics 41 (6) (2008) 065202. doi:10.1088/0022-3727/41/6/065202

[19] J. Chen, C. Schwenk, C. S. Wu, M. Rethmeier, Predicting the influence of groove angle on heat transfer and fluid flow for new gas metal arc welding processes, International Journal of Heat and Mass Transfer doi:10.1016/j.ijheatmasstransfer.2011.08.046

[20] Y. T. Cho, S.-J. Na, Application of Abel inversion in real-time calculations for circularly and elliptically symmetric radiation sources, Measurement Science and Technology 16 (3) (2005) 878–884. doi:10.1088/0957-0233/16/3/032

[21] D.-W. Cho, S.-J. Na, M.-H. Cho, J.-S. Lee, Simulations of weld pool dynamics in V-groove GTA and GMA welding, Welding in the World 57 (2) (2013) 223–233. doi:10.1007/s40194-012-0017-z

[22] D. W. Cho, S.-J. Na, M. H. Cho, J. S. Lee, A study on V-groove GMAW for various welding positions, Journal of Materials Processing Technology 213 (9) (2013) 1640–1652. doi:10.1016/j.jmatprotec.2013.02.015

[23] P. Sahoo, T. Debroy, M. J. McNallan, Surface tension of binary metal—surface active solute systems under conditions relevant to welding metallurgy, Metallurgical Transactions B 19 (3) (1988) 483–491. doi:10.1007/bf02657748

[24] Z. S. Saldi, Marangoni driven free surface flows in liquid weld pools, PhD dissertation, Delft University of Technology, Delft University of Technology (Dec. 2012). doi:https://doi.org/10.4233/uuid:8401374b-9e9c-4d25-86b7-fc445ec73d27
[25] K. C. Mills, Fe-316 stainless steel, in: Recommended Values of Thermophysical Properties for Selected Commercial Alloys, Elsevier, 2002, pp. 135–142. doi:10.1533/9781845690144.135

[26] C. S. Kim, Thermophysical properties of stainless steels, Technical Report ANL-75-55, Illinois, United States (sep 1975). doi:10.2172/4152287

[27] M. H. Cho, Y. C. Lim, D. F. Farson, Simulation of weld pool dynamics in the stationary pulsed gas metal arc welding process and final weld shape. Welding Journal 85 (12) (2006) 271s–283s. URL http://files.aws.org/wj/supplement/WJ_2006_12_s271.pdf

[28] V. R. Voller, C. R. Swaminathan, General source-based method for solidification phase change, Numerical Heat Transfer, Part B: Fundamentals 19 (2) (1991) 175–189. doi:10.1080/10407799108944962

[29] C. W. Hirt, B. D. Nichols, Volume of fluid (VOF) method for the dynamics of free boundaries, Journal of Computational Physics 39 (1) (1981) 201–225. doi:10.1016/0021-9991(81)90145-5

[30] V. R. Voller, C. Prakash, A fixed grid numerical modelling methodology for convection-diffusion mushy region phase-change problems, International Journal of Heat and Mass Transfer 30 (8) (1987) 1709–1719. doi:10.1016/0017-9310(87)90317-6

[31] A. Ebrahimi, C. R. Kleijn, I. M. Richardson, Sensitivity of numerical predictions to the permeability coefficient in simulations of melting and solidification using the enthalpy-porosity method, Energies 12 (22) (2019) 4360. doi:10.3390/en12224360

[32] J. U. Brackbill, D. B. Kothe, C. Zemach, A continuum method for modeling surface tension, Journal of Computational Physics 100 (2) (1992) 335–354. doi:10.1016/0021-9991(92)90240-y

[33] X. Bai, P. Colegrove, J. Ding, X. Zhou, C. Diao, P. Bridgeman, J. roman Hönige, H. Zhang, S. Williams, Numerical analysis of heat transfer and fluid flow in multilayer deposition of PAW-based wire and arc additive manufacturing, International Journal of Heat and Mass Transfer 124 (2018) 504–516. doi:10.1016/j.ijheatmasstransfer.2018.03.085

[34] S. Y. Lee, S. J. Na, A numerical analysis of a stationary gas tungsten welding arc considering various electrode angles. Welding Journal 75 (9) (1996) 269s–279s. URL http://files.aws.org/wj/supplement/WJ_1996_09_s269.pdf

[35] S. Y. Lee, S. J. Na, Analysis of TIG welding arc using boundary-fitted coordinates, Proceedings of the Institution of Mechanical Engineers, Part B: Journal of Engineering Manufacture 209 (2) (1995) 153–164. doi:10.1243/pime_proc_1995_209_067_02

[36] S. Unnikrishnakurup, S. Rouquette, F. Soulié, G. Fras, Estimation of heat flux parameters during static gas tungsten arc welding spot under argon shielding, International Journal of Thermal Sciences 114 (2017) 205–212. doi:10.1016/j.ijthermalsci.2016.12.008

[37] M. L. Lin, T. W. Eagar, Pressures produced by gas tungsten arcs, Metallurgical Transactions B 17 (3) (1986) 601–607. doi:10.1007/bf02670227

[38] N. S. Tsai, T. W. Eagar, Distribution of the heat and current fluxes in gas tungsten arcs, Metallurgical Transactions B 16 (4) (1985) 841–846. doi:10.1007/bf02687521
[39] A. Ebrahimi, C. R. Kleijn, I. M. Richardson, A simulation-based approach to characterise melt-pool oscillations during gas tungsten arc welding, International Journal of Heat and Mass Transfer 164 (2021) 120535. doi:10.1016/j.ijheatmasstransfer.2020.120535.

[40] A. Ebrahimi, C. R. Kleijn, M. J. M. Hermans, I. M. Richardson, The effects of process parameters on melt-pool oscillatory behaviour in gas tungsten arc welding, Journal of Physics D: Applied Physics 54 (27) (2021) 275303. doi:10.1088/1361-6463/abf808.

[41] J. W. Liu, Z. H. Rao, S. M. Liao, H. L. Tsai, Numerical investigation of weld pool behaviors and ripple formation for a moving GTA welding under pulsed currents, International Journal of Heat and Mass Transfer 91 (2015) 990–1000. doi:10.1016/j.ijheatmasstransfer.2015.08.046.

[42] K. C. Tsao, C. S. Wu, Fluid flow and heat transfer in GMA weld pools, Welding Journal 67 (3) (1988) 70s–75s. URL https://app.aws.org/wj/supplement/WJ_1988_03_s70.pdf

[43] Z. H. Rao, J. Zhou, S. M. Liao, H. L. Tsai, Three-dimensional modeling of transport phenomena and their effect on the formation of ripples in gas metal arc welding, Journal of Applied Physics 107 (5) (2010) 054905. doi:10.1063/1.3326163.

[44] G. Xu, J. Hu, H. L. Tsai, Three-dimensional modeling of arc plasma and metal transfer in gas metal arc welding, International Journal of Heat and Mass Transfer 52 (7-8) (2009) 1709–1724. doi:10.1016/j.ijheatmasstransfer.2008.09.018.

[45] M. Schnick, U. Fuessel, M. Hertel, M. Haessler, A. Spille-Kohoff, A. B. Murphy, Modelling of gas–metal arc welding taking into account metal vapour, Journal of Physics D: Applied Physics 43 (43) (2010) 434008. doi:10.1088/0022-3727/43/43/434008.

[46] A. B. Murphy, J. J. Lowke, Heat transfer in arc welding, in: Handbook of Thermal Science and Engineering, Springer International Publishing, 2018, pp. 2657–2727. doi:10.1007/978-3-319-26695-4_29.

[47] E. J. Soderstrom, K. M. Scott, P. F. Mendez, Calorimetric measurement of droplet temperature in GMAW, Welding Journal 90 (4) (2011) 77s–84s. URL http://files.aws.org/wj/supplement/wj201104_s77.pdf

[48] Q. Lin, X. Li, S. W. Simpson, Metal transfer measurements in gas metal arc welding, Journal of Physics D: Applied Physics 34 (3) (2001) 347–353. doi:10.1088/0022-3727/34/3/317.

[49] J. F. Lancaster (Ed.), The Physics of Welding, 2nd Edition, International series on materials science of technology, Pergamon Press, Oxford, UK, 1986.

[50] G. Zhang, G. Goett, R. Kozakov, D. Uhrlandt, U. Reisgen, K. Willms, R. Sharma, S. Mann, P. Lozano, Study of the arc voltage in gas metal arc welding, Journal of Physics D: Applied Physics 52 (8) (2018) 085202. doi:10.1088/1361-6463/aaf588.

[51] G. Zhang, G. Goett, D. Uhrlandt, P. Lozano, R. Sharma, A simplified voltage model in GMAW, Welding in the World 64 (9) (2020) 1625–1634. doi:10.1007/s40194-020-00943-x.

[52] A. Ebrahimi, C. R. Kleijn, I. M. Richardson, Numerical study of molten metal melt pool behaviour during conduction-mode laser spot melting, Journal of Physics D: Applied Physics 54 (2021) 105304. doi:10.1088/1361-6463/abca62.
[53] A. Ebrahimi, C. R. Kleijn, I. M. Richardson, The influence of surface deformation on thermocapillary flow instabilities in low Prandtl melting pools with surfactants, in: Proceedings of the 5th World Congress on Mechanical, Chemical, and Material Engineering, Avestia Publishing, 2019. doi:10.11159/htff19.201

[54] K. L. Johnson, T. M. Rodgers, O. D. Underwood, J. D. Madison, K. R. Ford, S. R. Whetten, D. J. Dagel, J. E. Bishop, Simulation and experimental comparison of the thermo-mechanical history and 3D microstructure evolution of 304L stainless steel tubes manufactured using LENS, Computational Mechanics 61 (5) (2017) 559–574. doi:10.1007/s00466-017-1516-y

[55] K. Sridharan, T. Allen, M. Anderson, G. Cao, G. Kulcinski, Emissivity of candidate materials for VHTR applications: Role of oxidation and surface modification treatments, Tech. rep. (jul 2011). doi:10.2172/1022709
URL https://www.osti.gov/biblio/1022709

[56] ANSYS Fluent, Release 19.2.
URL https://www.ansys.com/

[57] O. Ubbink, Numerical prediction of two fluid systems with sharp interfaces, PhD dissertation, Imperial College London (University of London), London, United Kingdom (Jan. 1997).
URL http://hdl.handle.net/10044/1/8604

[58] S. V. Patankar, Numerical Heat Transfer and Fluid Flow, 1st Edition, Taylor & Francis Inc, 1980.

[59] R. I. Issa, Solution of the implicitly discretised fluid flow equations by operator-splitting, Journal of Computational Physics 62 (1) (1986) 40–65. doi:10.1016/0021-9991(86)90099-9

[60] F. Wu, T. F. Flint, K. V. Falch, M. C. Smith, M. Drakopoulos, W. Mirihanage, Mapping flow evolution in gas tungsten arc weld pools, International Journal of Heat and Mass Transfer 179 (2021) 121679. doi:10.1016/j.ijheatmasstransfer.2021.121679

[61] C. X. Zhao, V. van Steijn, I. M. Richardson, C. R. Kleijn, S. Kenjeres, Z. Saldi, Unsteady interfacial phenomena during inward weld pool flow with an active surface oxide, Science and Technology of Welding and Joining 14 (2) (2009) 132–140. doi:10.1179/136217108x370281

[62] S. Kumar, A. S. Shahi, Effect of heat input on the microstructure and mechanical properties of gas tungsten arc welded AISI 304 stainless steel joints, Materials & Design 32 (6) (2011) 3617–3623. doi:10.1016/j.matdes.2011.02.017

[63] R. Unnikrishnan, K. S. N. S. Idury, T. P. Ismail, A. Bhadauria, S. K. Shekhawat, R. K. Khatirkar, S. G. Sapate, Effect of heat input on the microstructure, residual stresses and corrosion resistance of 304L austenitic stainless steel weldments, Materials Characterization 93 (2014) 10–23. doi:10.1016/j.matchar.2014.03.013

[64] G. Mohammed, M. Ishak, S. Aqida, H. Abdulhadi, Effects of heat input on microstructure, corrosion and mechanical characteristics of welded austenitic and duplex stainless steels: A review, Metals 7 (2) (2017) 39. doi:10.3390/met7020039