The Effects of Process Parameters on Melt-pool Oscillatory Behaviour in Gas Tungsten Arc Welding

Amin Ebrahimi\textsuperscript{1,†}, Chris R. Kleijn\textsuperscript{2}, Marcel J.M. Hermans\textsuperscript{1}, and Ian M. Richardson\textsuperscript{1}

\textsuperscript{1}Department of Materials Science and Engineering, Faculty of Mechanical, Maritime and Materials Engineering, Delft University of Technology, Mekelweg 2, 2628 CD Delft, The Netherlands
\textsuperscript{2}Department of Chemical Engineering, Faculty of Applied Sciences, Delft University of Technology, van der Maasweg 9, 2629 HZ Delft, The Netherlands
\textsuperscript{†}Corresponding author, Email: A.Ebrahimi@tudelft.nl

Abstract

Internal flow behaviour and melt-pool surface oscillations during arc welding are complex and not yet fully understood. In the present work, high-fidelity numerical simulations are employed to describe the effects of welding position, sulphur concentration (60–300 ppm) and travel speed (1.25–5 mm s\textsuperscript{-1}) on molten metal flow dynamics in fully-penetrated melt-pools. A wavelet transform is implemented to obtain time-resolved frequency spectra of the oscillation signals, which overcomes the shortcomings of the Fourier transform in rendering time resolution of the frequency spectra. Comparing the results of the present numerical calculations with available analytical and experimental datasets, the robustness of the proposed approach in predicting melt-pool oscillations is demonstrated. The results reveal that changes in the surface morphology of the pool resulting from a change in welding position alter the spatial distribution of arc forces and power-density applied to the molten material, and in turn affect flow patterns in the pool. Under similar welding conditions, changing the sulphur concentration affects the Marangoni flow pattern, and increasing the travel speed decreases the size of the pool and increases the offset between top and bottom melt-pool surfaces, affecting the flow structures (vortex formation) on the surface. Variations in the internal flow pattern affect the evolution of melt-pool shape and its surface oscillations.

Keywords: Fusion welding, Positional welding, Weld-pool behaviour, Surface oscillations, Numerical simulation
1 Introduction

Fusion joining of metallic materials is an essential requirement in many industries. The integrity of products depends critically on the joining technique employed and the quality of the joints produced [1, 2], which in turn is influenced by the dynamic stability of the melt-pool [3]. A better understanding of the complex transport phenomena inside melt-pools offers considerable opportunities for improved monitoring and control of joining processes. To date, control and optimisation of welding processes relies largely on trial-and-error experiments that often pose challenges due to the non-linearity of melt-pool responses to changes in operating conditions, material properties and process parameters [4, 5, 6]. Moreover, welding process development requires tolerance to parameter variations, within which the resultant weld integrity must be fit for the intended purpose, irrespective of the particular parameter combinations within the defined procedural range.

The present work focuses on positional gas tungsten arc (GTA) welding, which involves several significant operating parameters, the number increasing when complex time-dependent phenomena are considered [7]. The simulation-based approach utilised in the present work offers the potential to reduce procedure development costs and will enhance our understanding of melt-pool behaviour during positional GTA welding.

The majority of published studies on GTA melt-pool oscillatory behaviour are experimentally based, consider only the flat (1G or PA) welding orientation (i.e. position C1 shown in figure 1) and focus on processing the signals received from the melt pool to sense penetration [8]. Experimental techniques employed are often based on laser vision [9], arc voltage [10] or arc-light intensity [11] measurements. A critical limitation is related to the inadequate signal-to-noise ratio for low-amplitude surface oscillations [12], which makes the application of a triggering action essential [13]. Moreover, these techniques ignore convection in the melt pool, which is difficult to measure due to opacity, the fast dynamic response of the molten metal flow and high temperatures [1]. In addition to the experimental measurements, analytical models have been developed to predict dominant oscillation frequencies. These models are based on similarities between oscillations of the melt-pool surface and the vibrations of a thin stretched membrane [10, 14, 15, 11, 16]. Unfortunately, the absolute accuracy of these analytical models is critically dependent on the melt-pool shape, temperature-dependent material properties and processing conditions [12], which in turn are affected by unsteady transport phenomena in the pool [17]; factors that are not known a priori. Moreover, changes in oscillation mode and amplitude are not predictable using these models. Conversely, high-fidelity numerical simulations have demonstrated a remarkable potential to describe the complex internal flow behaviour in melt pools and associated surface deformations [18, 19].

Although many numerical models are available (e.g. [20, 21, 22, 23, 24, 25, 26, 27]), numerical studies on melt-pool surface oscillations are scarce and the melt-pool oscillatory behaviour is not yet fully understood, particularly for positional welding conditions. Previous studies often focused on the influence of surface deformations on the melt-pool shape [28, 29, 30, 31, 32] or the morphology of the melt-pool surface, to study ripple formation [33], welding defects such as undercut [34] or humping [35, 36, 37]. Chen et al. [38] revealed that the flow patterns in pools with one free surface, representative of partially penetrated welds, differ from those in pools with two free surfaces, representative of fully penetrated welds. In their model, they neglected solidification and melting, assumed that the flow inside the pool is axisymmetric and that the surface tension of the molten material is a linear function of temperature. Using a similar model, including electromagnetic forces and solid-liquid phase transformations, Ko et al. [39, 40] showed that pool oscillations during stationary GTA welding depend on the direction of the Marangoni flow. For many real-world
welding applications, non-pure materials are involved for which the surface tension changes non-linearly with temperature [41]. This coupled with movements of the melt-pool boundaries due to solid-liquid phase transformations form complex unsteady flow patterns that are inherently three-dimensional [42, 43, 44], affecting the melt-pool surface oscillations. In our previous study on stationary GTA welding [18], we demonstrated that changes in welding current and material properties can alter the time-frequency response of melt-pool oscillations under both partial and full penetration conditions. The welding position can also influence the shape of the deformations of the melt-pool surface and its shape during GTA welding [15, 46]. Moreover, morphology of the melt-pool surface can affect the spatial and temporal distribution of arc-pressure and power-density and thus the melt-pool dynamic behaviour [39, 18]. Further investigations are essential to broaden our understanding of complex internal flow behaviour in melt pools and melt-pool surface oscillations in positional GTA welding. In the present study, the results of numerical simulations employed to reveal complex unsteady transport phenomena in the melt-pool and associated surface oscillations during positional GTA welding are reported. The present study focuses particularly on fully-penetrated pools, where the melt-pool oscillations are critical for process stability; however, the present model is equally applicable to partial penetration conditions. The coupling between melt-pool surface deformations, arc force and power-density distributions, which represent physical realism and can affect the predicted thermal and flow fields, are taken into account. The continuous wavelet transform is applied to the time-resolved displacement signals acquired from the simulations to enhance our understanding of the evolution of surface oscillations and its correlation with process parameters and material properties. A novel insight into the evolution of melt-pool surface oscillations and the complex flow inside molten metal melt pools is provided, which offers a computational approach to fusion welding process development and optimisation.

2 Problem description

Molten metal flow behaviour and associated surface oscillations during positional gas tungsten arc welding of a stainless steel (AISI 316) plate are studied numerically. As shown schematically in figure 1, the plate has a thickness of $H_m = 2$ mm and is heated locally by an electric arc plasma to create a melt pool in the plate that is initially at $T_0 = 300$ K. A perpendicular torch is adopted for all positions in line with automated pipeline welding. The current is set to 85 A and the initial arc length (electrode tip to workpiece distance) before igniting the arc is $2.5$ mm. Obviously, the melt-pool surface deforms during the process, leading to changes in the length, voltage and power of the arc as well as power-density distribution and the magnitude and distribution of forces induced by the arc plasma [47, 48, 18]. In the present model, the melt-pool is decoupled from the arc plasma to reduce the computation time and complexity of simulations. Accordingly, the related source terms for momentum and thermal energy are adjusted dynamically in the present model to take these changes into account, as explained section 3. Argon gas is employed to shield the melt-pool. The temperature-dependent thermophysical properties of AISI 316 and argon are presented in table 1. The temperature-dependent surface tension of the molten material is modelled using an empirical correlation introduced in equation (21) Sahoo et al. [41], which accounts for the influence of surface-active elements (i.e. sulphur).

A moving reference frame is employed in the present numerical simulations to simulate unsteady convection in the melt pool. Hence, instead of moving the heat source, the material enters
the computational domain, translates at a fixed speed (i.e. the welding travel speed) opposite to the welding direction shown in figure 1 and leaves the computational domain. Applying the moving reference frame technique facilitates a decrease in the size of the computational domain and thus the runtime. The computational domain is designed in the form of a rectangular cuboid encompassing the workpiece and two layers of gas below and above the sample to monitor the oscillations of the melt-pool surfaces. The width and the length of the computational domain is $W = 40\,\text{mm}$ and $L = 70\,\text{mm}$ respectively, which is substantially larger than the dimensions of the melt-pool. The boundary conditions applied to the computational domain in the simulations are shown in figure 1(b). The outer boundaries of the workpiece are adiabatic and are treated as no-slip moving walls. The gas layers have a thickness of $H_a = 2\,\text{mm}$ and a fixed atmospheric pressure ($p = p_{\text{atm}} = 101.325\,\text{kPa}$) is applied to their outer boundaries. The electrode axis is located in the middle of the plate (i.e. $x = W/2$) and 15 mm away from the leading-edge of the plate (i.e. $y = 15\,\text{mm}$). Eight different welding positions (workpiece orientations with respect to gravity) are studied, as shown in figure 1(c).

Figure 1: Schematic of moving gas tungsten arc welding (GTAW). (a) a cross section of the plate showing a fully-penetrated melt pool, (b) three-dimensional view of the problem under consideration and (c) different welding positions studied in the present work.
Table 1: Thermophysical properties of AISI 316 and argon used in the computational model. T is in Kelvin.

| Property                        | Stainless steel (AISI 316) [49] | Gas (argon) | Unit       |
|---------------------------------|---------------------------------|-------------|------------|
| Density \( \rho \)             | 7100                            | 1.623       | kg \text{m}^{-3} |
| Specific heat capacity \( c_p \) | 430.18 +0.1792 \cdot T (solid phase) | 520.64      | J \text{kg}^{-1} \text{K}^{-1} |
|                                 | 830 (liquid phase)              |             |            |
| Thermal conductivity \( k \)    | 11.791 +0.0131 \cdot T (solid phase) | 520.64      | W \text{m}^{-1} \text{K}^{-1} |
|                                 | 6.49 +0.0129 \cdot T (liquid phase) |             |            |
| Viscosity \( \mu \)            | 6.42 \times 10^{-3}            | 2.1 \times 10^{-5} | kg \text{m}^{-1} \text{s}^{-1} |
| Thermal expansion coefficient \( \beta \) | 2 \times 10^{-6} |             | K^{-1}     |
| Latent heat of fusion \( \mathcal{L} \) | 2.7 \times 10^{5} |             | J \text{kg}^{-1} |
| Liquidus temperature \( T_l \)  | 1723                            |             | K          |
| Solidus temperature \( T_s \)   | 1658                            |             | K          |

3 Methods

3.1 Model formulation

Our previous multiphase model for stationary GTA welding [18] is extended here to predict the complex three-dimensional molten metal flow and melt-pool surface oscillations during positional moving GTA welding. In this model, the molten metal and argon were assumed to be incompressible and were treated as Newtonian fluids. Accordingly, the unsteady governing equations were cast in conservative form as follow:

\[
\nabla \cdot \mathbf{u} = 0, \quad (1)
\]

\[
\rho \frac{D \mathbf{u}}{Dt} = \mu \nabla^2 \mathbf{u} - \nabla p + \mathbf{F}_d + \mathbf{F}_s + \mathbf{F}_b, \quad (2)
\]

\[
\rho \frac{D h}{Dt} = \frac{k}{c_p} \nabla^2 h - \rho \frac{D (\psi \mathcal{L})}{Dt} + S_q + S_l, \quad (3)
\]

where, \( \rho \) is the density, \( \mathbf{u} \) the relative velocity vector, \( t \) 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, and \( (\psi \mathcal{L}) \) the latent heat. The total enthalpy of the material \( \mathcal{H} \) is the sum of the latent heat \( (\psi \mathcal{L}) \) and the sensible heat \( h \) and is defined as follows [50]:

\[
\mathcal{H} = (h_r + \int_{T_r}^{T} c_p dT) + \psi \mathcal{L}, \quad (4)
\]

where, \( T \) is the temperature, \( \psi \) the local liquid volume-fraction, and \( \mathcal{L} \) 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 [50], its value can be calculated as follows:

\[
\psi = \frac{T - T_s}{T_l - T_s}; \quad T_s \leq T \leq T_l, \quad (5)
\]
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 \[51\] was employed, 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:

$$\frac{D\phi}{Dt} + \phi \nabla \cdot \mathbf{u} = 0.$$ \hspace{1cm} (6)

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

$$\xi = \phi \xi_m + (1 - \phi) \xi_g,$$ \hspace{1cm} (7)

where, $\xi$ corresponds to thermal conductivity $k$, specific heat capacity $c_p$, viscosity $\mu$ and 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 \[52\], was incorporated into the momentum equation and is defined as

$$F_d = C \left(\frac{1 - \psi}{\psi^3 + \epsilon}\right) u,$$ \hspace{1cm} (8)

where, $C$ is the mushy-zone constant and its value was chosen to equal $10^7$ kg m$^{-2}$ s$^{-2}$, in accordance with \[53\] to avoid numerical artefacts associated with inappropriate assignment of this parameter, and $\epsilon$ is a constant equal to $10^{-3}$ employed to avoid division by zero.

To apply forces on the gas-metal interface, the continuum surface force (CSF) model \[54\] was 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 $F_s$ was added to equation (2) as follows:

$$F_s = f_s \|\nabla \phi\| \frac{2\rho}{\rho_m + \rho_g},$$ \hspace{1cm} (9)

where, the surface force applied to a unit area $f_s$ comprises arc plasma, surface tension and Marangoni forces and was defined as follows:

$$f_s = f_s + \gamma \kappa \hat{n} + \frac{d\gamma}{dT} \left[\nabla T - \hat{n} (\hat{n} \cdot \nabla T)\right],$$ \hspace{1cm} (10)

where, $f_s$ 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 $f_{\tau}$ and arc pressure $f_p$,

$$f_s = f_{\tau} + f_p.$$ \hspace{1cm} (11)

The arc plasma shear stress $f_{\tau}$, which applies tangent to the surface, was defined as follows \[55\]:

...
\[ f_r = \lbrack \tau_{\text{max}} g_r (R, \sigma_r) \rbrack \hat{t}, \quad (12) \]

where, the maximum arc shear stress \( \tau_{\text{max}} \) \([56, 57]\), the arc shear stress distribution function \( g_r \) \([58]\) and the surface unit tangent vector \( \hat{t} \) \([55]\) were defined as follows:

\[ \tau_{\text{max}} = 7 \times 10^{-2} I^{1.5} \exp \left( -2.5 \times 10^4 \ell \right), \quad (13) \]
\[ g_r (R, \sigma_r) = \sqrt{\frac{R}{\sigma_r}} \exp \left( \frac{-R^2}{\sigma_r^2} \right), \quad (14) \]
\[ \hat{t} = \frac{r - \hat{n} (\hat{n} \cdot r)}{\|r - \hat{n} (\hat{n} \cdot r)\|}. \quad (15) \]

Here, \( I \) is the welding current in Amperes, \( \ell \) is 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 \( \ell \) and current \( I \) and was approximated on the basis of the data reported by Lee and Na \([56]\):

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

The arc pressure \( f_p \) was determined as follows \([48]\):

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

where, \( I \) is the current in Amperes, 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 \([47]\) for an argon arc with an electrode tip angle of 75° as follows:

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

where, \( \ell \) is the local arc length in meters, and \( I \) the current in Amperes. Hence, spatial and temporal variations of the arc pressure distribution resulted from changes in morphology of the melt-pool surface were taken into account. 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 \( \left( \mu_0 I^2 / 4\pi \right) \) due to changes in \( \|\nabla \phi\| \) \([18, 59]\). This numerical artefact was negated by incorporating \( \mathcal{F}_p \), which is defined as follows:

\[ \mathcal{F}_p = \alpha \frac{\mu_0 I^2}{4\pi} \frac{1}{\int \int \|f_p\| dV}. \quad (19) \]

The dimensionless factor \( \alpha \) was employed, as suggested by Liu \textit{et al.} \([32]\) and Lin and Eagar \([48]\), to match the theoretically determined arc pressure with experimentally measured values, and was calculated as follows:

\[ \alpha = 3 + 8 \times 10^{-3} I, \quad (20) \]
with \( I \) the welding current in Amperes.

The temperature-dependent surface tension of the molten material was modelled using an empirical correlation\(^{[41]}\) that accounts for the influence of sulphur as a surface-active element, and is defined as follows:

\[
\gamma = \gamma_m^o + \left( \frac{\partial \gamma}{\partial T} \right)^o \left( T - T_m \right) - R \Gamma_s \ln \left[ 1 + \psi a_s \exp \left( \frac{-\Delta H^o}{RT} \right) \right], \tag{21}
\]

where, \( \gamma_m^o \) is the surface tension of the pure molten-material at the melting temperature \( T_m \), \( \left( \frac{\partial \gamma}{\partial T} \right)^o \) the temperature gradient of the surface tension of the pure molten-material, \( T \) the temperature in Kelvin, \( \Gamma_s \) the adsorption at saturation, \( R \) the universal gas constant, \( \psi \) an entropy factor, \( a_s \) the activity of the solute, and \( \Delta H^o \) the standard heat of adsorption. The values reported by Sahoo et al.\(^{[41]}\) were used for the properties in equation (21). Variations of the surface tension of a molten Fe-S alloy with temperature are shown in figure 2 for different sulphur concentrations studied in the present work. It should be noted that in this model it is assumed that sulphur is distributed uniformly over the melt-pool surface. Since temperatures in the melt-pool are below the boiling temperature of stainless steel (\( \Theta(3000 \text{ K}) \)) and there is no material addition with a different sulphur concentration, the effects of changes in the sulphur concentration is virtually negligible for the cases studied in the present work. Therefore, the effects of adsorption, desorption and redistribution of species along the melt-pool surface are neglected in the present numerical simulations.

**Figure 2:** Surface tension of a molten Fe-S alloy as a function of temperature approximated using equation (21) for different sulphur concentrations.

\( \mathbf{F_b} \) in equation (2) is the body force, which comprises electromagnetic, gravity and thermal buoyancy forces. The electromagnetic force was computed using the model proposed by Tsao and Wu\(^{[60]}\) transformed into a body-fitted coordinate system, and the thermal buoyancy force was modelled using the Boussinesq approximation\(^{[61]}\). Hence, the body forces are defined as follows:

\[
f_{bx} = -\frac{-\mu_0 I^2}{4\pi^2 \sigma_e^2 R} \exp \left( -\frac{R^2}{2 \sigma_e^2} \right) \left[ 1 - \exp \left( -\frac{R^2}{2 \sigma_e^2} \right) \right] \left( 1 - \frac{z - z'}{H_m - z'} \right)^2 \left( \frac{x}{R} \right), \tag{22}
\]

\[
f_{by} = -\frac{-\mu_0 I^2}{4\pi^2 \sigma_e^2 R} \exp \left( -\frac{R^2}{2 \sigma_e^2} \right) \left[ 1 - \exp \left( -\frac{R^2}{2 \sigma_e^2} \right) \right] \left( 1 - \frac{z - z'}{H_m - z'} \right)^2 \left( \frac{y}{R} \right), \tag{23}
\]
\[ \mathbf{f}_{bz} = -\frac{\mu_0 I^2}{4\pi^2 \sigma_e^2 R} \exp\left(\frac{-R^2}{2\sigma_e^2}\right) \left(1 - \frac{z - z'}{H_m - z'}\right) + \rho g - \rho \beta (T - T_l) \mathbf{g}. \] (24)

Here, the distribution parameter for the electromagnetic force \( \sigma_e \) is the same as \( \sigma_p \), according to Tsai and Eagar [47], \( z' \) is the position of the melt-pool surface in \( x-y \) plane, \( \beta \) the thermal expansion coefficient, and \( \mathbf{g} \) the gravitational acceleration vector.

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

\[ S_q = \mathcal{G}_q \left[ \frac{\eta I U}{2\pi \sigma_q^2} \exp\left(\frac{-R^2}{2\sigma_q^2}\right) \left\| \nabla \phi \right\| \frac{2 \rho c_p}{(\rho c_p)_m + (\rho c_p)_g} \right], \] (25)

where, the process efficiency \( \eta \) considered to be a linear function of welding current, varying from 80% at 50 A to 70% at 300 A [62]. It should be noted that the source term \( S_q \) is only applied to the top surface of the workpiece. The arc voltage \( U \) depends on welding current and arc length, and was determined as follows:

\[ U = U_o + U_1 I + U_e \hat{I}, \] (26)

where, \( U_o \) is the electrode fall voltage equal to 8 V [63], \( U_1 \) the coefficient of variation of arc voltage with current equal to \( 1.3 \times 10^{-2} \) V A\(^{-1} \) [64], and \( U_e \) the electric field strength equal to 7.5 V cm\(^{-1} \) [63]. Using the data reported by Tsai and Eagar [47], the distribution parameter \( \sigma_q \) (in meters) was determined as follows:

\[ \sigma_q = 1.61 \times 10^{-1} \ell^{0.976} + 2.23 \times 10^{-4} I^{0.395}, \] (27)

with \( \ell \) in meters and \( I \) in Amperes. The adjustment factor \( \mathcal{G}_q \) was used to negate changes in the total heat input due to surface deformations, which is defined as follows:

\[ \mathcal{G}_q = \frac{\eta I U}{\int V S_q dV}. \] (28)

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

\[ S_l = - [h_c (T - T_0) + \kappa_b \varepsilon (T^4 - T_0^4)] \left\| \nabla \phi \right\| \frac{2 \rho c_p}{(\rho c_p)_m + (\rho c_p)_g}, \] (29)

where, \( h_c \) is the heat transfer coefficient equal to 25 W m\(^{-2} \) K\(^{-1} \) [65], \( \kappa_b \) the Stefan–Boltzmann constant and \( \varepsilon \) the radiation emissivity equal to 0.45 [66].

### 3.2 Numerical implementation

The numerical simulations reported in the present work make use of a finite-volume solver, ANSYS Fluent [67]. The surface-tension model as well as the source terms in the governing equations were implemented in the solver using user-defined functions programmed in the C programming language. The computational grid contains about \( 5.2 \times 10^6 \) hexahedral cells, where cell spacing varies gradually from 40 \( \mu \)m in the melt-pool region and close to the gas-metal interface to 400 \( \mu \)m close to the boundaries of the computational domain. Spatial discretisation was implemented...
by the central-differencing scheme for momentum advection and diffusive fluxes, and the time
marching was performed employing a first order implicit scheme. The time-step size was set
to $10^{-5}$ s, resulting in a Courant number ($Co = \|u\|\Delta t/\Delta x$) less than 0.25. Velocity and pressure
fields were coupled using the PISO (pressure-implicit with splitting of operators) scheme \[68\] and
the pressure interpolation was performed employing the PRESTO (pressure staggering option)
scheme \[69\].
The advection of the volume-fraction field was formulated using an explicit compressive
VOF method \[70\]. Convergence for each time-step is achieved when scaled residuals fall below $10^{-7}$.
Each simulation was executed in parallel on 80 cores (AMD EPYC 7452) of a computing cluster
for a total run-time of about 800 h. To reduce the computational costs associated with running
the model, possible developments can focus on performing model order reduction or decreasing
the spatial and temporal resolutions of the simulations. Reliability, validity and grid independence
of the present numerical model in predicting internal flow behaviour, evolution of the melt-pool
shape and surface oscillations were meticulously verified in our previous works \[71, 53, 72, 18\].
Moreover, the frequencies acquired from the present three-dimensional numerical simulations deviate
less than 10\% from the experimental data reported by Yudodibroto \[73\].

### 3.3 Time-Frequency analysis

Because of the complex unsteady molten metal flow in the melt pool, the frequency spectra of
signals received from an oscillating melt-pool during GTA welding are often time-variant. The dynamic
features of the oscillation signals cannot be disclosed employing the conventional Fourier
transform (FT) analysis \[74\]. The continuous wavelet transform (CWT) \[75\] was employed in
the present study to overcome the shortcomings of the conventional fast Fourier transform (FFT)
analysis in characterising the non-stationary features of the signals that may contain abrupt changes
in their frequency spectra. Employing the CWT method, the time-resolved melt-pool surface
oscillation signals obtained from the numerical simulations can be decomposed into time and
frequency spaces simultaneously. The principle of signal processing based on the wavelet transform
is described in \[75\] and is not repeated here. The Python programming language was employed
to perform time-frequency analysis using the oscillation signals acquired at a sampling frequency
of $10^5$ Hz. The Morlet wavelet function, which is a Gaussian-windowed complex sinusoid, was used
as the mother wavelet that yields an adequate balance in both time and frequency domains.

### 4 Results

In this section, the effects of different welding positions (shown in figure 1(c)) as well as travel speed
(ranging between 1.25 mm s$^{-1}$ and 5 mm s$^{-1}$) and sulphur concentration (ranging between 60 ppm
and 300 ppm) on internal flow behaviour, evolution of the melt-pool shape and surface oscillations
during GTA welding are described. Displacement signals required for characterising the molten
metal oscillations were recorded from several monitoring points distributed over the melt-pool surface
during the simulations, and are shown in figure 3 for welding position C1. Although the amplitudes
of oscillations are different at different locations, the frequency spectra obtained from FFT analysis
look similar. Hence, the signals recorded from the monitoring point $m(x, y, z) = m(0, 0, z_{interface})$
in the period of $t = 0$ s to 10 s were analysed utilising the continuous wavelet transform. It should be
noted that no triggering action (such as welding current pulsation) was taken to excite melt-pool
surface oscillations in the present numerical simulations because even small surface fluctuations are
detectable using the proposed computational approach [18].

Figure 3: The displacement signals recorded from the monitoring points on the melt-pool top surface and the corresponding frequency spectra for welding position C1. (a and b) displacement signals obtained from the simulations, (c and d) frequency spectra obtained from FFT analysis. Magnitudes in frequency spectrum are normalised with respect to the maximum magnitude. Positive values of $\delta$ indicate surface depression and its negative values indicate surface elevation. ($I = 85$ A, travel speed: $2.5$ mm s$^{-1}$ and sulphur concentration: 240 ppm)

The oscillation signals of the melt-pool surface and the corresponding frequency spectra for different welding positions with $I = 85$ A and sulphur concentration of 240 ppm are shown in figure 4 for downward welding direction (C1–C4) and in figure 5 for upward welding direction (C5–C8). For the cases with a travel speed of $2.5$ mm s$^{-1}$, the melt-pool depth increases over time and reaches the plate thickness $H_m$ in about 1.25 s. Then, the melt-pool surface area on the bottom surface increases and becomes almost the same as that on the top surface (i.e. full penetration) at $t \approx 4$ s, after which the melt-pool grows to reach a quasi-steady state. In the case of GTA welding in position C1 (figure 4(a)), the frequency of oscillations decreases from $f \approx 73$ Hz at $t = 1.25$ s to values of about 32 Hz at $t = 4$ s, while the melt-pool surface area on the bottom surface is increasing to establish full penetration. After full penetration is established, the frequency of the most energetic event increases to $f \approx 45$ Hz, as indicated by arrow, and subsequently decreases gradually to values of about 32 Hz as time passes and reaches 10 s. Both low and high frequencies remain in the spectrum after full penetration. The amplitudes of oscillations also augment as the melt-pool size increases. The frequency of oscillations obtained from the present numerical simulations for welding position C1 agrees reasonably (within 10% deviation bands) with the experimental data reported by Li et al. [76]. The range of oscillation frequencies predicted for different welding positions (C1–C8) seems to be the same, however the results suggest that the welding position affects the amplitudes of oscillations and the evolution of melt-pool oscillatory behaviour. The frequency of oscillations varies from $f \approx 25$ Hz to values of about 37 Hz after full penetration for welding positions C2 and C4. Such increase in the frequency of oscillations after full penetration also occurs for the welding position C3, however the frequency decreases after about 1 s and the melt pool oscillates at low frequencies up to $t \approx 7$ s, after which the melt pool collapses (i.e. burns-through).
The displacement signals recorded from the monitoring point \( m(x, y, z) = m(0, 0, z_{\text{interface}}) \) and the corresponding time-frequency spectra for different welding positions with downward welding direction. (a) welding positions C1, (b) welding positions C2, (c) welding positions C3 and (d) welding positions C4. Magnitudes are normalised with respect to the maximum magnitude in the time-frequency spectrum. Positive values of \( \delta \) indicate surface depression and its negative values indicate surface elevation. (\( I = 85 \) A, travel speed: 2.5 mm s\(^{-1}\) and sulphur concentration: 240 ppm)

The amplitude of oscillations predicted for cases C5–C8 are generally larger than those predicted for cases C1–C4, as shown in figure 5. For the welding position C5, the frequency of oscillations decreases from 66 Hz to 52 Hz within 1 s (from \( t = 1.25 \) s to \( t = 2.25 \) s) and then the frequency of oscillations increases to 64 Hz, as indicated by arrow. For the cases that welding direction is upward (C6–C8), multiple changes occur in the frequency of oscillations. The abrupt changes observed in the frequency domain indicate the importance of utilising the wavelet transform instead of the Fourier transform for analysing the behaviour of oscillating melt-pools. Changes in the frequency...
of oscillations relate to changes in flow pattern in the melt pool, the shape and size of the melt-pool, and surface tension of the molten metal \[14, 18\], which is discussed in section 5.

**Figure 5:** The displacement signals recorded from the monitoring point \( m(x, y, z) = m(0, 0, z_{\text{interface}}) \) and the corresponding time-frequency spectrum for different welding positions with upward welding direction. (a) welding positions C5, (b) welding positions C6, (c) welding positions C7 and (d) welding positions C8. Magnitudes are normalised with respect to the maximum magnitude in the time-frequency spectrum. Positive values of \( \delta \) indicate surface depression and its negative values indicate surface elevation. \((I = 85\,A, \text{travel speed: } 2.5\,\text{mm s}^{-1} \text{ and sulphur concentration: } 240\,\text{ppm})\)

Changes in the sulphur concentration of the material, as a surface-active element, can result in notable changes in surface tension of the molten material and its variation with temperature \( (\partial \gamma / \partial T) \), as shown in figure 2. Reducing the amount of sulphur in the material results in intensifying the outward fluid flow over the melt-pool surface (as shown in figure 11), forming a wide melt pool.
The effect of sulphur concentration of the material on the evolution of the frequency of oscillations is shown in figure 6 for welds in position C1 and travel speed of 2.5 mm s\(^{-1}\). The evolution of oscillation frequency is almost the same for the cases with sulphur concentrations of 120 ppm and 60 ppm, and differs both qualitatively and quantitatively from that of the case containing 240 ppm sulphur. The melt pool oscillates at higher frequencies with larger amplitudes in the case with sulphur concentration of 240 ppm compared to the cases with sulphur concentrations of 120 ppm and 60 ppm. Moreover, the fundamental frequency of oscillations does not increase markedly after full penetration in the cases with sulphur concentrations of 120 ppm and 60 ppm, as it does in the case with sulphur concentration of 240 ppm. This difference in the evolution of oscillations frequency relates to changes in the structure of the molten metal flow in the melt pool and thus melt-pool shape evolution.

![Figure 6](image)

**Figure 6:** The influence of sulphur concentration on the time-frequency spectra of melt-pool oscillations during GTA welding. (a) sulphur concentration: 120 ppm, and (b) sulphur concentration: 60 ppm. Magnitudes are normalised with respect to the maximum magnitude in the time-frequency spectrum. Positive values of \(\delta\) indicate surface depression and its negative values indicate surface elevation. (\(I = 85\) A, welding position: C1 and travel speed: 2.5 mm s\(^{-1}\)).

The influence of travel speed on the frequency of melt-pool surface oscillations is shown in figure 7 for the welds in position C1 and sulphur concentration of 240 ppm. When the travel speed was set to 1.25 mm s\(^{-1}\), the melt pool reaches full penetration at \(t \approx 3\) s and the amplitude of surface oscillations start to increase, as shown in figure 7(a). For the case with a travel speed of 1.25 mm s\(^{-1}\), the frequency of oscillations gradually decreases from 69 Hz at \(t = 1.25\) s to 29 Hz at \(t = 10\) s while the melt-pool is growing over time. Increasing the travel speed from 1.25 mm s\(^{-1}\) to 5 mm s\(^{-1}\), the amplitude of oscillations decreases significantly. Moreover, the frequency of oscillations decreases up to \(t = 4\) s while the melt-pool size is increasing. Afterwards, the melt-pool reaches a quasi-steady-state condition and the variation of the melt-pool size over time becomes insignificant, resulting surface oscillations at an almost constant frequency of about 41 Hz. It should be noted that when the pool size has reached steady state, the surface area of the melt pool on the bottom surface is consistently smaller than that on the top-surface in the case with a travel speed of 5 mm s\(^{-1}\).
Figure 7: The influence of welding travel speed on the time-frequency spectra of melt-pool oscillations during GTA welding. (a) travel speed: 1.25 mm s$^{-1}$, and (b) travel speed: 5 mm s$^{-1}$. Magnitudes are normalised with respect to the maximum magnitude in the time-frequency spectrum. Positive values of $\delta$ indicate surface depression and its negative values indicate surface elevation. ($I = 85$ A, welding position: C1 and sulphur concentration: 240 ppm)

5 Discussion

The frequency of oscillations predicted using the present numerical simulations are compared to the analytical approximations calculated from the model developed by Maruo and Hirata [16] for fully-penetrated melt pools in welding position C1, and the results are presented in figure 8. This analytical model is expressed mathematically as follows [16]:

$$
\frac{1}{2\pi} \sqrt{\frac{2 \bar{\gamma} k^2}{\rho H_m} - \frac{2 \|g\|}{H_m}},
$$

(30)

where, $H_m$ is the plate thickness, $\rho$ the density of the molten metal and $\bar{\gamma}$ the average surface tension of the molten material. The mean value of surface tension for the alloy considered in the present study is approximately 1.6 N m$^{-1}$ in the temperature range of 1723–2500 K [41]. The value of $k$ in equation (30) depends on both melt-pool size and oscillation mode and is obtained from the following equations [73]:

Mode 3: $k = 2.405 \left( \frac{D_e}{2} \right)^{-1}$, \hspace{1cm} (31)

Mode 2f: $k = 3.832 \left( \frac{D_e}{2} \right)^{-1}$, \hspace{1cm} (32)

where, $D_e$ is the equivalent diameter of the melt-pool under full penetration condition defined as follows:
where, \( l \) and \( w \) are the melt-pool length and width on the top surface respectively, as shown in figure 8. The analytical model of Maruo and Hirata [16] is developed for fully-penetrated melt pools, assuming that the melt-pool shape and size on the top and bottom surfaces of the workpiece are the same with no offset. It appears that for the case with a travel speed of 2.5 mm s\(^{-1}\) and sulphur concentration of 240 ppm the melt-pool surface oscillations follow the analytically predicted frequencies in mode 3 for \( t > 4 \) s when the surface area of the melt-pool on the bottom surface approaches the surface area on the top surface. Reducing travel speed to 1.25 mm s\(^{-1}\) while keeping the sulphur concentration unchanged (240 ppm), the melt-pool surface area on the bottom surface becomes almost the same as on the top surface and the predicted frequencies follow the analytical predictions in mode 3. For the case with a travel speed of 5 mm s\(^{-1}\) and sulphur concentration of 240 ppm, the melt pool shape and size reach a quasi-steady-state with an equivalent diameter \( D_e \approx 7.5 \) mm that does not change notably over time. In many practical applications, the melt-pool surface area is not necessarily the same on the top and bottom surfaces of the workpiece, particularly when the melt pool is growing before reaching a quasi-steady-state. Moreover, there is often an offset between the positions of the melt-pool surfaces on the top and bottom of the workpiece, which increases with increasing the travel speed. These differences in the melt-pool size and shape, as well as the offset between the melt-pool surfaces on the top and bottom surfaces of the workpiece, lead to deviation of the frequencies approximated using the analytical models from those predicted from numerical simulations and measured experimentally. The frequencies predicted using the present numerical simulations are in reasonably good agreement (within 10% deviation bands) with the experimental measurements of fully penetrated pools reported by Yudodibroto [73].

Despite the suitability of analytical models for predicting the frequency of oscillations under a full penetration condition, they fail to predict changes in oscillation mode during welding processes, particularly when the melt pool is evolving over time. Moreover, variation in the value of surface tension of the molten material with temperature is ignored in the analytical models, which limits their accuracy in predicting the frequency of oscillations during GTA welding. The results presented in figure 8 also demonstrate that changes in the sulphur concentration can affect the frequency of oscillations and their evolution due to variations in the internal flow pattern and thus the melt-pool shape, which results from changes in the Marangoni stresses acting on the melt-pool surfaces.
Figure 8: Variation of the frequency of oscillations for fully-penetrated melt-pools during GTA welding as a function of equivalent melt-pool diameter. The frequency of oscillations obtained from the present numerical simulations (unfilled symbols), experimental data reported by Yudodibroto [73] (squares), and analytical approximations using the model proposed by Maruo and Hirata [16] for oscillation frequencies in Mode 3 (up and down bulk motion, dark grey line) and Mode 2f (sloshy oscillation, light grey line). ($I = 85$ A and welding position: C1)

Figure 9 shows the evolution of the thermal and flow fields over the melt-pool surfaces as well as the pool shape during GTA welding in position C1 with a travel speed of 2.5 mm s$^{-1}$ and sulphur concentration of 240 ppm. A melt pool forms soon after the arc ignition, grows and its depth reaches the plate thickness after about 1.25 s, forming a fully-penetrated melt pool. Fluid flow in the melt pool is driven by various time-variant forces acting on the molten material such as Marangoni, Lorentz, arc plasma shear and pressure and buoyancy forces, resulting in a complex flow pattern that is inherently three-dimensional. This fluid motion transfers the heat absorbed by the material and affects the melt-pool shape and its evolution over time. The relative contribution of advective to diffusive energy transfer can be evaluated using the Péclet number ($Pe = \rho c_p D_u ||u||/k$), which is larger than unity ($\mathcal{O}(100)$) for the cases studied in the present work, signifying the notable influence of advection on the melt-pool shape.

The results shown in figure 9 indicate that the maximum temperature of the melt pool after reaching a quasi-steady-state condition varies between 2260 K and 2340 K and the maximum local fluid velocity varies between 0.21 m s$^{-1}$ and 0.34 m s$^{-1}$. Inward fluid flow from the boundary to the centre of the pool is observed over the top surface that meets an outward flow in the central region. This change in the flow direction is due to the sign change of the temperature gradient of surface tension ($\partial \gamma / \partial T$) at a specific temperature, as shown in figure 2. Interactions between these
two streams disturb the thermal field over the pool surface and generate an unsteady complex flow pattern inside the pool, affecting the energy transport in the melt pool and thus the evolution of the melt-pool shape. Temperatures are below the critical temperature over the bottom surface of the pool and the temperature gradient of the surface tension is positive all over the surface, resulting in inward fluid flow from the melt-pool boundaries. Moreover, two vortices form over the top surface as time passes that generate a periodic asymmetry in the flow field, leading to flow oscillations around the melt-pool centreline. The formation of such vortices occurs because of the fluid motion from the front part of the pool toward the rear and collision with the inward flow in the rear part of the pool. A similar flow pattern was observed during GTA welding by Zhao et al. \[43\] using particle image velocimetry (PIV).

The outward fluid flow in the central region of the melt pool coupled with the arc pressure applied to the molten material leads to melt-pool surface depressions in the front and central region of the melt pool. Variation in the flow pattern over time, as well as changes in the melt-pool shape result in changes in oscillatory behaviour. For the case shown in figure 9, the melt-pool surface area on the bottom surface of the workpiece increases over time and becomes almost the same as that on the top surface after 4 s. This change in pool shape coupled with the fluid flow that evolves over the bottom surface, causes a change in the melt-pool oscillatory behaviour, as reflected in figure 4. Variations in the pool surface morphology also result in variation of the power-density and arc force distribution over the surface and the total power input from the electric arc, enhancing flow disturbances.

![Figure 9: Evolution of the melt-pool shape during gas tungsten arc welding. Contours show the temperature distribution over the melt-pool surface and are overlaid with velocity vectors. The 0.5 m s\(^{-1}\) reference vector is provided for scaling the velocity field. \((I = 85\) A, welding position: C1, travel speed: 2.5 mm s\(^{-1}\) and sulphur concentration: 240 ppm)](image-url)

Variation in the welding position can affect melt-pool surface deformations, resulting in changes
in the power-density and arc force distribution over the melt-pool surface and thus the evolution of the melt-pool shape and its oscillatory behaviour. The influence of welding position on melt-pool shape is shown in figure 10 for cases with a travel speed of 2.5 mm s$^{-1}$ and sulphur concentration of 240 ppm. When welding downward (C2–C4), the molten material is pulled by the gravitational force towards the front part of the melt pool and forms a bulge beneath the welding torch. This change in the melt-pool surface morphology decreases the average arc length and alters the power-density distribution, through changing the distribution parameter $\sigma_q$ (equation (27)), and thus temperature profile over the surface. The change in the power-density distribution in turn increases the temperature gradients over the melt-pool surface and thus the magnitude of Marangoni forces. The results reveal that the molten material moves from the rear of the melt pool towards the front around the centreline, resulting in further reduction of the melt-pool thickness in the rear part of the melt pool, which can eventually lead to the rupture of liquid layer and melt-pool collapse if the dynamic force balance cannot be maintained. In contrast, when welding upward (C6–C8), the molten material moves towards the rear part of the melt pool because of the gravitational force, forming a concavity in the front part of the pool. The formation of this concavity increases the average arc length beneath the welding torch and the distribution parameter $\sigma_q$ (equation (27)). This increase in the distribution parameter reduces the temperature gradients over the melt pool surface and thus the magnitude of Marangoni forces. These results suggest that although the melt-pool shape and its surface morphology are influenced by the welding position, the overall flow structure in the melt pool, which is dominated by Marangoni and electromagnetic forces, is not affected significantly by the gravitational force [46]. The results in figure 10 also show that the material thickness $H$ reduces locally beneath the welding torch with the increase in the average arc length in the cases that welding direction is upward (C6–C8). This reduction in the material thickness results in the establishment of full penetration somewhat earlier at $t \approx 3$ s compared to that of the case C1 and C5, changing the oscillation frequency as reflected in figure 5. The results suggest that when the relative material thickness ($H/H_m$) beneath the welding torch reduces to values less than about 0.65, an unsteady multicellular flow pattern evolves in the pool [18, 77], leading to irregular surface deformations that are reflected in the time-frequency spectra shown in figure 5(b and d) for $t > 6$ s.
Figure 10: The influence of the welding position on evolution of the melt-pool shape during gas tungsten arc welding. Contours show the temperature profile at $t = 7$ s and are overlaid with velocity vectors. Cross sections on the $y$-$z$ plane are located at $x = 0$. ($I = 85$ A, travel speed: $2.5$ mm s$^{-1}$ and sulphur concentration: $240$ ppm)

Figure 11 shows the melt-pool shape and thermal and flow fields over the melt-pool surfaces at $t = 10$ s for GTA welding in welding position C1 with a travel speed of $2.5$ mm s$^{-1}$ and different sulphur concentrations in the material. The results of the present numerical simulations suggest that both the amplitude and frequency of oscillations decrease with reducing the sulphur concentration in the material. Reducing sulphur concentration in the material results in an increase in the average surface tension of the molten material, affects the variation of surface tension with temperature ($\partial \gamma / \partial T$) and reduces the critical temperature at which the sign of the temperature gradient of surface tension ($\partial \gamma / \partial T$) changes, according to equation (21) (see figure 2). The flow pattern over the melt-pool top surface becomes mostly outward when the sulphur concentration in the material is reduced. The increasingly outward fluid flow weakens disturbances caused by the interaction of inward and outward fluid flows over the melt-pool surface. Additionally, the outward fluid flow transfers the heat absorbed by the material towards the melt-pool boundary, reducing the maximum temperature and the magnitude of temperature gradients over the melt-pool surface. Moreover, the outward flow results in a relatively larger melt-pool surface area on the top surface compared to that on the bottom surface and fluid velocities decrease on the bottom surface. These changes in the flow field alter the melt-pool shape, as shown in figure 11 and in turn affects the melt-pool oscillatory behaviour. A notable change in the time-frequency spectra of the cases with sulphur concentrations of $120$ ppm and $60$ ppm (figure 6) compared to that with a sulphur concentration of $240$ ppm (figure 4(a)) is that the frequency of oscillations decreases gradually over time and does not change suddenly as observed in figure 4(a) at $t \approx 4$ s.
Figure 11: The influence of sulphur concentration in the material on the melt-pool shape during gas tungsten arc welding. Contours show the temperature profile at $t = 10$ s and are overlaid with velocity vectors. The $0.5 \text{ m s}^{-1}$ reference vector is provided for scaling the velocity field. ($I = 85 \text{ A}$, travel speed: $2.5 \text{ mm s}^{-1}$ and welding position: C1)

The influence of travel speed on melt-pool shape and heat and fluid flow in the melt pool during GTA welding in position C1 is shown in figure 12 for the cases with a sulphur concentration of $240 \text{ ppm}$. Increasing the travel speed, while keeping other process parameters the same, results in a decrease in the melt-pool size, which is due to the reduction of nominal heat input to the material. Additionally, the melt-pool shape changes from a virtually circular shape to a teardrop shape with increasing travel speed. The flow pattern obtained from the numerical simulations also reveals that vortex structures do not form over the melt-pool surface for the case with a travel speed of $1.25 \text{ mm s}^{-1}$, in contrast to other cases with higher travel speeds. Moreover, the offset between the top and bottom melt-pool surfaces increases with increasing the travel speed. The offset measured at the leading edge of the pool with respect to the $z$-axis increases from about $5^\circ$ at a travel speed of $1.25 \text{ mm s}^{-1}$ to values of about $50^\circ$ at a travel speed of $5 \text{ mm s}^{-1}$. 
Figure 12: The influence of travel speed on the melt-pool shape during gas tungsten arc welding. Contours show the temperature profile at $t = 10$ s and are overlaid with velocity vectors. The $0.5 \text{ m s}^{-1}$ reference vector is provided for scaling the velocity field. ($I = 85$ A, sulphur concentration: 240 ppm and welding position: C1)

6 Conclusions

High-fidelity three-dimensional numerical simulations were performed to study the oscillatory behaviour of fully-penetrated melt pools during positional gas tungsten arc welding. The influence of welding positions, the sulphur concentration in the material and travel speed on complex unsteady convection in the melt pool and oscillations of the melt-pool surface were investigated. The frequencies predicted using the present computational model are compared with analytical and experimental data, and reasonably good agreement (within 5% deviation bands) is achieved. Using the present numerical approach, evolutions of the melt-pool surface oscillations during GTA welding are described by revealing the unsteady complex flow pattern in the melt pools and subsequent changes in the melt-pool shape, which are generally difficult to visualise experimentally. Moreover, evolution of the frequency of melt-pool oscillations during arc welding are not predictable using the analytical models that are available in the open literature.

Melt-pool oscillatory behaviour depends on surface tension of the molten material and shape and size of the melt-pool. Changes in material properties and welding process parameters affect convection in the melt pool, hydrodynamic instabilities that arise and resultant variations in
the melt-pool shape. Depending on the processing condition, these instabilities can also grow in
time, affecting melt-pool stability and may even lead to melt-pool collapse and process failure.
Welding position affects the melt-pool surface morphology, altering the spatial distribution of arc
forces and power-density applied to the molten material and thus changes flow pattern in the melt-
pool. The change in the flow pattern affects the evolution of the melt-pool shape and its oscillatory
behaviour. The frequency of oscillations seems to vary within the same range \(22 \text{Hz} < f < 73 \text{Hz}\)
for different welding positions studied in the present work, however the evolution of oscillation
frequencies, which depends on the melt-pool shape, is affected by welding position. Under similar
welding conditions, sulphur concentration in the material significantly affects the thermal and flow
fields in the melt pool and consequently the shape of the pool, changing the oscillatory behaviour
of the melt-pool. Increasing the travel speed decreases the melt-pool size, increases the offset
between top and bottom melt-pool surfaces and also affects the flow structures (vortex formation)
on the melt-pool surface. These observations offer an insight into the complex melt-pool oscillatory
behaviour during positional gas tungsten arc welding and suggest that the processing window for
advanced fusion-based manufacturing processes can be determined by utilising numerical simulations
that can potentially reduce the costs associated with process development and optimisation.

Acknowledgement

This research was carried out under project number F31.7.13504 in the framework of the Partnership
Program of the Materials innovation institute M2i (www.m2i.nl) and the Foundation for Fundamental
Research on Matter (FOM) (www.fom.nl), which is part of the Netherlands Organisation for Scientific
Research (www.nwo.nl). The authors would like to thank the industrial partner in this project
“Allseas Engineering B.V.” for the financial support.

Author Contributions

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

Conflict of interest

The authors declare no conflict of interest.

Data availability

The raw/processed data required to reproduce these findings cannot be shared at this time due to
their large size, but representative samples of the research data are presented in the paper. Other
datasets generated during this study are available from the corresponding author on reasonable
request.
References

[1] 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

[2] 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

[3] T. DebRoy, H. L. Wei, J. S. Zuback, T. Mukherjee, J. W. Elmer, J. O. Milewski, A. M. Beese, A. Wilson-Heid, A. De, W. Zhang, Additive manufacturing of metallic components – process, structure and properties, Progress in Materials Science 92 (2018) 112–224. doi:10.1016/j.pmatsci.2017.10.001

[4] K. C. Mills, B. J. Keene, Factors affecting variable weld penetration, International Materials Reviews 35 (1) (1990) 185–216. doi:10.1179/095066090790323966

[5] S. C. Juang, Y. S. Tarng, Process parameter selection for optimizing the weld pool geometry in the tungsten inert gas welding of stainless steel, Journal of Materials Processing Technology 122 (1) (2002) 33–37. doi:10.1016/s0924-0136(02)00021-3

[6] R. Cunningham, C. Zhao, N. Parab, C. Kantzos, J. Pauza, K. Fezzaa, T. Sun, A. D. Rollett, Keyhole threshold and morphology in laser melting revealed by ultrahigh-speed x-ray imaging, Science 363 (6429) (2019) 849–852. doi:10.1126/science.aav4687

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

[8] C. Li, Y. Shi, L. Du, G. Yufen, M. Zhu, Real-time measurement of weld pool oscillation frequency in GTAW-P process, Journal of Manufacturing Processes 29 (2017) 419–426. doi:10.1016/j.jmapro.2017.08.011

[9] Y. Shi, G. Zhang, X. J. Ma, Y. F. Gu, J. K. Huang, D. Fan, Laser-vision-based measurement and analysis of weld pool oscillation frequency in GTAW-P, Welding Journal 94 (5) (2015) 176s–187s.

[10] Y. H. Xiao, G. den Ouden, A study of GTA weld pool oscillation, Welding Journal 69 (8) (1990) 289s–293s.

[11] C. D. Yoo, R. W. Richardson, An experimental study on sensitivity and signal characteristics of weld pool oscillation, Transactions of the Japan Welding Society 24 (2) (1993) 54–62. URL https://ci.nii.ac.jp/naid/110003379951/en/

[12] A. S. Tam, D. E. Hardt, Weld pool impedance for pool geometry measurement: Stationary and nonstationary pools, Journal of Dynamic Systems, Measurement, and Control 111 (4) (1989) 545–553. doi:10.1115/1.3153090

[13] Y. H. Xiao, Weld pool oscillation during gas tungsten arc welding PhD dissertation, Delft University of Technology (1992). URL http://resolver.tudelft.nl/uuid:f91da1e6-1a17-4223-9d73-419f1ac9c312

[14] Y. H. Xiao, G. den Ouden, Weld pool oscillation during GTA welding of mild steel, Welding Journal 72 (1993) 428s–434s.

[15] K. Andersen, G. E. Cook, R. J. Barnett, A. M. Strauss, Synchronous weld pool oscillation for monitoring and control, IEEE Transactions on Industry Applications 33 (2) (1997) 464–471. doi:10.1109/28.668011

[16] H. Maruo, Y. Hirata, Natural frequency and oscillation modes of weld pools. 1st report: Weld pool oscillation in full penetration welding of thin plate, Welding International 7 (8) (1993) 614–619. doi:10.1080/09507119309548457
[17] F. Wu, K. V. Falch, D. Guo, P. English, M. Drakopoulos, W. Mirihanage, Time evolved force domination in arc weld pools, Materials & Design 190 (2020) 108534. doi:10.1016/j.matdes.2020.108534
[18] 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
[19] 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
[20] S. Kou, D. K. Sun, Fluid flow and weld penetration in stationary arc welds, Metallurgical Transactions A 16 (1) (1985) 203–213. doi:10.1007/bf02815302
[21] T. Zacharia, A. H. Eraslan, D. K. Aidun, S. A. David, Three-dimensional transient model for arc welding process, Metallurgical Transactions B 20 (5) (1989) 645–659. doi:10.1007/bf02655921
[22] C. S. Wu, L. Dorn, Computer simulation of fluid dynamics and heat transfer in full-penetrated TIG weld pools with surface depression, Computational Materials Science 2 (2) (1994) 341–349. doi:10.1016/0927-0256(94)90116-3
[23] C. S. Wu, J. Chen, Y. M. Zhang, Numerical analysis of both front- and back-side deformation of fully-penetrated GTAW weld pool surfaces, Computational Materials Science 39 (3) (2007) 635–642. doi:10.1016/j.commatsci.2006.08.018
[24] S. Mishra, T. J. Lienert, M. Q. Johnson, T. DebRoy, An experimental and theoretical study of gas tungsten arc welding of stainless steel plates with different sulfur concentrations, Acta Materialia 56 (9) (2008) 2133–2146. doi:10.1016/j.actamat.2008.01.028
[25] A. Traidia, F. Roger, Numerical and experimental study of arc and weld pool behaviour for pulsed current GTA welding, International Journal of Heat and Mass Transfer 54 (9-10) (2011) 2163–2179. doi:10.1016/j.ijheatmasstransfer.2010.12.005
[26] J. Mougenot, J.-J. Gonzalez, P. Freton, M. Masquère, Plasma–weld pool interaction in tungsten inert-gas configuration, Journal of Physics D: Applied Physics 46 (13) (2013) 135206. doi:10.1088/0022-3727/46/13/135206
[27] H. Hao, J. Gao, H. Huang, Numerical simulation for dynamic behavior of molten pool in tungsten inert gas welding with reserved gap, Journal of Manufacturing Processes 58 (2020) 11–18. doi:10.1016/j.jmapro.2020.07.063
[28] M. E. Thompson, J. Szekely, The transient behavior of weldpools with a deformed free surface, International Journal of Heat and Mass Transfer 32 (6) (1989) 1007–1019. doi:10.1016/0017-9310(89)90003-3
[29] M. C. Tsai, S. Kou, Marangoni convection in weld pools with a free surface, International Journal for Numerical Methods in Fluids 9 (12) (1989) 1503–1516. doi:10.1002/fld.1650091206
[30] S. D. Kim, S. J. Na, Effect of weld pool deformation on weld penetration in stationary gas tungsten arc-welding, Welding Journal 71 (5) (1992) 179s–193s.
[31] Y. M. Zhang, Z. N. Cao, R. Kovacevic, Numerical analysis of fully penetrated weld pools in gas tungsten arc welding, Proceedings of the Institution of Mechanical Engineers, Part C: Journal of Mechanical Engineering Science 210 (2) (1996) 187–195. doi:10.1243/pime_proc_1996_210_185_02
[32] Z. N. Cao, Y. M. Zhang, R. Kovacevic, Numerical dynamic analysis of moving GTA weld pool, Journal of Manufacturing Science and Engineering 120 (1) (1998) 173–178. doi:10.1115/1.2830096
[33] 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
[34] X. Meng, G. Qin, X. Bai, Z. Zou, Numerical analysis of undercut defect mechanism in high speed gas tungsten arc welding, Journal of Materials Processing Technology 236 (2016) 225–234. doi:10.1016/j.jmatprotec.2016.05.020

[35] C. Feng, G. Qin, X. Meng, P. Geng, Defect evolution of 409L stainless steel in high-speed TIG welding, Materials and Manufacturing Processes 35 (2) (2020) 179–186. doi:10.1080/10426914.2020.1711925

[36] J. Du, G. Zhao, Z. Wei, Effects of welding speed and pulse frequency on surface depression in variable polarity gas tungsten arc welding of aluminum alloy, Metals 9 (2) (2019) 114. doi:10.3390/met9020114

[37] J. Pan, S. Hu, L. Yang, D. Wang, Investigation of molten pool behavior and weld bead formation in VP-GTAW by numerical modelling, Materials & Design 111 (2016) 600–607. doi:10.1016/j.matdes.2016.09.022

[38] Y. Chen, S. A. David, T. Zacharia, C. J. Cremers, Marangoni convection with two free surfaces, Numerical Heat Transfer, Part A: Applications 33 (6) (1998) 599–620. doi:10.1080/10407789808913957

[39] S. H. Ko, C. D. Yoo, D. F. Farson, S. K. Choi, Mathematical modeling of the dynamic behavior of gas tungsten arc weld pools, Metallurgical and Materials Transactions B 31 (6) (2000) 1465–1473. doi:10.1007/s11663-000-0031-1

[40] S. H. Ko, S. K. Choi, C. D. Yoo, Effects of surface depression on pool convection and geometry in stationary GTA weld, Welding Journal 80 (2) (2001) 39s–45s.

[41] 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

[42] Y. Joshi, P. Dutta, P. E. Schupp, D. Espinosa, Nonaxisymmetric convection in stationary gas tungsten arc weld pools, Journal of Heat Transfer 119 (1) (1997) 164–172. doi:10.1115/1.2824082

[43] 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

[44] A. Kidess, S. Kenjereš, C. R. Kleijn, The influence of surfactants on thermocapillary flow instabilities in low prandtl melting pools, Physics of Fluids 28 (6) (2016) 062106. doi:10.1063/1.495397

[45] N. Kang, T. A. Mahank, A. K. Kulkarni, J. Singh, Effects of gravitational orientation on surface deformation and weld pool geometry during gas tungsten arc welding, Materials and Manufacturing Processes 18 (2) (2003) 169–180. doi:10.1080/10407782.2016.1264747

[46] M. C. Nguyen, M. Medale, O. Asserin, S. Gounand, P. Gilles, Sensitivity to welding positions and parameters in GTA welding with a 3D multiphysics numerical model, Numerical Heat Transfer, Part A: Applications 71 (3) (2017) 233–249. doi:10.1080/10407782.2016.1264747

[47] 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/bf02667521

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

[49] K. C. Mills, Fe-304 stainless steel, in: Recommended Values of Thermophysical Properties for Selected Commercial Alloys, Elsevier, 2002, pp. 127–134. doi:10.1533/9781845690144.127

[50] 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:10.1080/1040779910894962

[51] 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
[52] 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

[53] 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

[54] 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

[55] X. Bai, P. Colegrove, J. Ding, X. Zhou, C. Diao, P. Bridgeman, J. roman Hönigge, 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

[56] 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.

[57] 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/plme_proc.1995.209.067.02

[58] S. Unnikrishnakurup, S. Rouquette, F. Soulé, 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

[59] X. Meng, G. Qin, Z. Zou, Investigation of humping defect in high speed gas tungsten arc welding by numerical modelling, Materials & Design 94 (2016) 69–78. doi:10.1016/j.matdes.2016.01.019

[60] K. C. Tsao, C. S. Wu, Fluid flow and heat transfer in GMA weld pools, Welding journal 67 (3) (1988) 70s–75s.

[61] D. J. Tritton, Physical Fluid Dynamics, Springer Netherlands, 1977. doi:10.1007/978-94-009-9992-3

[62] 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

[63] I. M. Richardson, Properties of the constricted gas tungsten (plasma) welding arc at elevated pressures, Ph.D. thesis, Industrial and Manufacturing Science, Cranfield Institute of Technology (1991).

[64] M. Goodarzi, R. Choo, J. M. Toguri, The effect of the cathode tip angle on the GTAW arc and weld pool: I. mathematical model of the arc, Journal of Physics D: Applied Physics 30 (19) (1997) 2744–2756. doi:10.1088/0022-3727/30/19/013

[65] 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

[66] 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., University of Wisconsin (jul 2011). doi:10.2172/1022709 URL https://www.osti.gov/biblio/1022709

[67] ANSYS® Fluent, Release 19.2.

[68] 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
[69] S. V. Patankar, Numerical Heat Transfer and Fluid Flow, 1st Edition, Taylor & Francis Inc, 1980.

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

[71] 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

[72] 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 (10) (2020) 105304. doi:10.1088/1361-6463/abca62

[73] Y. B. Yudodibroto, Liquid metal oscillations and arc behaviour during welding. PhD dissertation, Delft University of Technology (2010).
URL http://resolver.tudelft.nl/uuid:dcae1f78-9186-4161-ad88-711f27781335

[74] O. Rioul, M. Vetterli, Wavelets and signal processing, IEEE Signal Processing Magazine 8 (4) (1991) 14–38. doi:10.1109/79.91217

[75] S. Mallat, A Wavelet Tour of Signal Processing, Elsevier Inc., 2009. doi:10.1016/B978-0-12-374370-1.X0001-8

[76] C. Li, Y. Shi, Y. Gu, P. Yuan, Monitoring weld pool oscillation using reflected laser pattern in gas tungsten arc welding, Journal of Materials Processing Technology 255 (2018) 876–885. doi:10.1016/j.jmatprotec.2018.01.037

[77] M. F. Schatz, G. P. Neitzel, Experiments on thermocapillary instabilities, Annual Review of Fluid Mechanics 33 (1) (2001) 93–127. doi:10.1146/annurev.fluid.33.1.93