Postprint: Numerical Simulation of Heat Transfer and Fluid Flow in Twin-Electrode TIG Arc-Weld Pool

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Abstract

For the twin-electrode TIG arc heat source, based on an established unified three-dimensional mathematical model of the twin-electrode arc-weld pool, simulations were performed using SUS304 stainless steel as the base material to obtain the distributions of temperature, velocity, current density, magnetic flux density, and electromagnetic force in both the twin-electrode TIG arc and the weld pool. The simulation results show good agreement with existing experimental results. The buoyancy force, electromagnetic force, plasma flow drag, Marangoni shear stress, and turbulence effects acting on the weld pool were considered, the distribution of heat input to the weld pool and the variation of shear stress on the pool surface were analyzed, and the weld pool flow and heat transfer under the individual action of each force were compared respectively. Simultaneously, the relative strengths of heat conduction and heat convection in the weld pool were compared using the dimensionless Peclet number (Pe). The results indicate that the non-axisymmetric characteristics of the twin-electrode arc lead to non-axisymmetric distributions of current density, heat flux density, plasma flow drag, and Marangoni shear stress on the weld pool surface, ultimately forming a non-axisymmetric weld pool geometry, while the development and evolution of the weld pool have no significant effect on arc behavior. Compared with the TIG arc, the plasma flow drag of the twin-electrode TIG arc is significantly reduced. Marangoni shear stress determines the flow state of the stainless steel weld pool, and convective heat transfer dominates the heat transfer in the stainless steel weld pool. The combined action of these two factors determines the heat transfer process in the weld pool and is the fundamental cause for the formation of different weld pool geometries.

Full Text

Numerical Simulation of Heat Transfer and Fluid Flow in Double Electrode TIG Arc-Weld Pool

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Abstract

Based on a developed unified three-dimensional (3D) mathematical model that includes both the double tungsten electrodes arc and weld pool for a double electrode TIG arc heat source, the temperature, velocity, current density, magnetic flux density, and electromagnetic force distributions of the double electrode TIG arc and weld pool were obtained for SUS304 stainless steel. The simulated results show fair agreement with available experimental data. The model accounts for buoyancy, electromagnetic force, plasma drag force, Marangoni shear stress, and turbulent effects to formulate weld pool behavior, and the influence of each force on weld pool flow was studied individually. The heat flux and shear stress distributions at the weld pool surface were also analyzed. A dimensionless number Pe was employed to compare the relative importance of convective versus conductive heat transfer in the weld pool. The results demonstrate that the non-axisymmetric characteristics of the double electrode arc lead to non-axisymmetric distributions of current density, heat flux, plasma drag force, and Marangoni shear stress at the weld pool surface, ultimately producing non-axisymmetric weld pool profiles. The evolution of the weld pool has minimal effect on arc behavior. The plasma drag force of the double tungsten electrode TIG arc decreases significantly compared with conventional TIG arcs. Marangoni shear stress determines the flow pattern in the stainless steel weld pool, and convective heat transfer dominates the heat transfer process. The combined effect of these two factors governs the heat transfer in the weld pool and represents the fundamental mechanism responsible for the formation of different weld pool geometries.

KEY WORDS double electrode, numerical simulation, heat transfer, non-axisymmetric, dimensionless number

Tungsten inert gas (TIG) welding is widely used in industrial applications, but its current-carrying capacity is limited, resulting in shallow penetration per pass and consequently low welding efficiency. In contrast, double electrode TIG arcs can carry larger currents under the same tungsten electrode current capacity, thereby increasing welding heat input and improving efficiency. As a novel heat source, double electrode TIG arcs have received limited research attention. Kobayashi et al. [1] implemented double TIG welding in the fabrication of large natural gas storage tanks. In arc-assisted active TIG welding (AA-TIG), a coupled arc with a small amount of O_2 mixed into the shielding gas alters the temperature coefficient of surface tension in the weld pool, significantly increasing penetration while mitigating defects such as humping and undercutting that occur during high-current, high-speed welding [2,3]. Leng et al. [4] and Huang et al. [5] experimentally investigated the effects of process parameters on arc pressure. Zhang et al. [6,7] measured the temperature distribution of coupled arcs using water-cooled copper as the anode, finding that the distribution was no longer rotationally symmetric and that temperatures were lower than those of conventional TIG arcs under equivalent conditions.

Although preliminary experimental studies on double tungsten electrode TIG arcs exist, the small dimensions of welding arcs and weld pools pose significant challenges for experimental investigation. Moreover, experimental results provide limited real-time information about the welding process, making it difficult to understand the underlying physical mechanisms. Numerical simulation offers an effective approach to address these challenges [8]. However, traditional welding simulations have focused either solely on the weld pool by assuming certain distributions for heat flux, current density, and arc pressure at the weld pool surface [9–13], or primarily on the arc itself [14–16] with assumptions about cathode current density distribution [15,16] that are only applicable to axisymmetric cases.

In reality, the arc and weld pool constitute a mutually influential unified system. Choo et al. [17,18] developed a coupled arc-weld pool model where the weld pool' s current density and heat input were derived from arc solutions. Since the arc and weld pool were calculated separately, the weld pool computation could not account for plasma drag force effects, nor could it consider the influence of the weld pool on arc behavior. With continuous theoretical improvements and rapid advances in computational technology, research on coupled arc-weld pool numerical simulations has gradually increased. Tanaka et al. [19] and Murphy et al. [20] employed 2D axisymmetric models to investigate TIG arc-weld pool interactions, including treatment of the electrode sheath regions and the effects of metal vapor on arc behavior. Lei et al. [21] and Lu et al. [22] focused on the free surface of the weld pool, while Lu et al. [23] used an approach similar to Choo et al. [17,18] to study the effects of active elements on weld pool flow and geometry. Yin et al. [24] utilized a 3D model to investigate arc and weld pool behavior under axial magnetic fields. Mougenot et al. [25] examined the 3D interaction between TIG arcs and the anode. For multi-electrode arc welding, Ogino et al. [26] established a 3D model of double TIG arcs to study the characteristics of the double arc heat source but did not consider weld pool formation. Ding et al. [27] developed a 2D model to numerically analyze double TIG arc characteristics without considering the effects on the base metal. Kanemaru et al. [28] created a 3D model for TIG-MIG hybrid welding that included both the arc and base metal, focusing primarily on hybrid arc behavior.

Building upon previous research [29,30], this work further investigates the heat transfer and flow behavior in double tungsten electrode TIG arc-weld pools. Using the CFD software FLUENT, we calculated the temperature, velocity, current density, magnetic flux density, and electromagnetic force distributions in both the double tungsten electrode arc and weld pool. For AA-TIG welding of stainless steel, we studied the individual effects of buoyancy, electromagnetic force, plasma drag force, and Marangoni shear stress on the weld pool. The influence of weld pool behavior on arc characteristics was considered. By comparing the shear stress distributions and heat input at the weld pool surface under Ar shielding and Ar+O_2 shielding, and analyzing changes in weld pool flow and heat transfer patterns, we reveal the mechanisms responsible for weld pool geometry formation in double electrode TIG and AA-TIG welding.

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1 Mathematical Model

For coupled arc AA-TIG welding, based on the research of Jönsson et al. [31], the effect of small amounts of O_2 (4% by volume) mixed into the shielding gas on the welding arc can be neglected. Experiments show that although slight tungsten electrode erosion occurs due to oxygen, it has no significant effect on the welding process. To simplify the model, only the influence of O_2 on weld pool surface tension is considered here. The mathematical model is simplified with the following assumptions: (1) The arc is a continuous medium in local thermodynamic equilibrium (LTE); (2) The arc is optically thin, meaning reabsorption of plasma radiation is negligible compared to radiation losses across all wavelengths; (3) The arc is a steady-state argon arc at atmospheric pressure; (4) Heat losses due to arc viscosity effects and gravitational effects are neglected; (5) The Boussinesq approximation is used for buoyancy in the weld pool; (6) The weld pool free surface is planar, and metal vapor effects are neglected.

1.1 Conservation Equations

(1) Continuity Equation

$$\frac{\partial \rho}{\partial t} + \nabla \cdot (\rho \mathbf{v}) = 0$$

where t is time, ρ is density, and $\mathbf{v}$ is the velocity vector.

(2) Momentum Conservation Equation

$$\frac{\partial(\rho\mathbf{v})}{\partial t} + \nabla\cdot(\rho\mathbf{v}\mathbf{v}) = -\nabla P + \mathbf{j}\times\mathbf{B} + \rho\mathbf{g} + \nabla\cdot\tau$$

where P is pressure, $\mathbf{j}$ is current density, $\mathbf{B}$ is magnetic flux density, $\mathbf{g}$ is gravitational acceleration, and τ is the viscous stress tensor. In Cartesian coordinates:

$$\tau_{ij} = \mu\left(\frac{\partial v_i}{\partial x_j} + \frac{\partial v_j}{\partial x_i}\right) - \frac{2}{3}\mu\frac{\partial v_k}{\partial x_k}\delta_{ij}$$

where μ is dynamic viscosity, and v_i and v_j are velocity components in the x_i and x_j directions, respectively. The enthalpy-porosity method [32] is used to describe the base metal melting process, with an additional momentum source term $\mathbf{S}_u$ to describe flow in the mushy zone:

$$\mathbf{S}_u = -C\frac{(1-f_l)^2}{f_l^3 + B}\mathbf{v}$$

where C is a relatively large constant, B is a small number to prevent division by zero, and f_l is the liquid volume fraction, assumed to vary linearly with temperature:

$$f_l = \begin{cases} 0 & T < T_s \\ \frac{T-T_s}{T_l-T_s} & T_s \le T \le T_l \\ 1 & T > T_l \end{cases}$$

where T_s and T_l are the solidus and liquidus temperatures of the weld pool, respectively. In the arc region, $\mathbf{S}_u = 0$.

(3) Energy Conservation Equation

$$\frac{\partial(\rho h)}{\partial t} + \nabla\cdot(\rho\mathbf{v}h) = \nabla\cdot\left(\frac{k}{c_P}\nabla h\right) + S_h$$

where T is temperature, c_P is specific heat at constant pressure, k is thermal conductivity, and S_h is the source term in the energy conservation equation. In the anode region, the latent heat of fusion during weld pool formation is considered, yielding an additional heat source term:

$$S_h = S_{h,\mathrm{arc}} + S_{h,\mathrm{pool}}$$

where $S_{h,\mathrm{pool}}$ is the weld pool energy source term and L is the latent heat of fusion for stainless steel. In the arc region, S_h is expressed as:

$$S_{h,\text{arc}} = \frac{j^2}{\sigma} + \frac{5k_B}{2e}\mathbf{j} \cdot \nabla T - S_R$$

where $S_{h,\text{arc}}$ is the arc energy source term, σ is electrical conductivity, k_B is the Boltzmann constant, e is the elementary charge, and S_R is the net radiation loss per unit volume. These three terms represent Joule heating, electron enthalpy transport, and net radiation loss, respectively.

(4) Current Continuity Equation

$$\nabla \cdot (\sigma \nabla \Phi) = 0$$

where Φ is electric potential.

(5) Ohm' s Law

$$\mathbf{j} = -\sigma \nabla \Phi$$

where $\mathbf{E}$ is electric field intensity.

(6) Magnetic Vector Potential Poisson Equation

$$\nabla^2 \mathbf{A} = -\mu_0 \mathbf{j}$$

where $\mathbf{A}$ is the magnetic vector potential and μ_0 is the permeability of free space. The electromagnetic force $\mathbf{j} \times \mathbf{B}$ can be calculated from $\mathbf{B} = \nabla \times \mathbf{A}$.

(7) K- Turbulence Model Conservation Equations

Based on studies by Choo and Szekely [33], Hong et al. [34], and Goodarzi et al. [35], turbulence in the weld pool must be considered. The standard two-equation K- turbulence model [36] is employed here, with transport equations for turbulent kinetic energy K and turbulent dissipation rate ε:

$$\frac{\partial(\rho K)}{\partial t} + \nabla \cdot (\rho \mathbf{v} K) = \nabla \cdot (\mu_t \nabla K) + \rho G_K - \rho\varepsilon$$

$$\frac{\partial(\rho \varepsilon)}{\partial t} + \nabla \cdot (\rho \mathbf{v} \varepsilon) = \nabla \cdot (\mu_t \nabla \varepsilon) + c_1 \frac{\varepsilon}{K}\rho G_K - c_2 \rho \frac{\varepsilon^2}{K}$$

The turbulent kinetic energy generation rate G_K is given by:

$$G_K = \mu_t \left\{ 2\left[\left(\frac{\partial u}{\partial x}\right)^2 + \left(\frac{\partial v}{\partial y}\right)^2 + \left(\frac{\partial w}{\partial z}\right)^2\right] + \left(\frac{\partial u}{\partial y} + \frac{\partial v}{\partial x}\right)^2 + \left(\frac{\partial u}{\partial z} + \frac{\partial w}{\partial x}\right)^2 + \left(\frac{\partial v}{\partial z} + \frac{\partial w}{\partial y}\right)^2 \right\}$$

Turbulent viscosity μ_t is calculated as:

$$\mu_t = c_\mu \rho \frac{K^2}{\varepsilon}$$

Turbulent thermal conductivity k_t is computed from:

$$k_t = \frac{c_P \mu_t}{\mathrm{Pr}_t}$$

where Pr_t is the turbulent Prandtl number. The empirical constants in equations (13)-(16) are: $c_\mu = 0.09$, $c_1 = 1.44$, $c_2 = 1.92$, $\sigma_K = 1.0$, and $\sigma_\varepsilon = 1.3$.

1.2 Boundary Conditions

1.2.1 External Boundaries The computational domain and boundary conditions are shown in [Figure 1: see original paper]. The tungsten electrode diameter is 2.4 mm, the electrode tip angle is 60°, the top platform is 0.3 mm, the arc length is 3 mm, the base metal thickness is 8 mm, and the computational domain diameter is 20 mm. Region A is the gas inlet with specified velocity and temperature distributions; region B is the gas outlet; regions C and D are the outer surfaces of the base metal with heat exchange conditions to the surroundings:

$$q_{\mathrm{mix}} = q_{\mathrm{conv}} + q_{\mathrm{rad}} = -h_c(T - T_\infty) - \varepsilon_r \sigma (T^4 - T_\infty^4)$$

where q_{mix} is the total heat loss, q_{conv} and q_{rad} are heat losses due to convection and radiation, respectively, h_c is the convective heat transfer coefficient, ε_r is emissivity, σ is the Stefan-Boltzmann constant, and T_∞ is the ambient temperature. Region E is the tungsten electrode cross-section with specified current density and temperature. Based on temperature measurements of TIG welding electrodes by Matsuda et al. [37], the temperature is set to 1800 K. Specific boundary conditions are listed in .

1.2.2 Internal Boundaries At the tungsten electrode-arc interface, coupled conditions are applied for temperature and electromagnetic fields (see references [30, 38, 39]), while a no-slip condition is used for momentum.

The anode heat flux q_a is:

$$q_a = -k \frac{\partial T}{\partial z} + |j_z| \Phi_a - \varepsilon_r \sigma (T^4 - T_\infty^4) = q_c + q_e + q_r$$

which consists of three components: conductive heat q_c due to temperature gradient, latent heat of condensation q_e from electrons entering the anode, and surface radiation loss q_r. Here, j_z is the current density in the z direction and Φ_a is the anode work function, taken as 4.65 V [14].

The anode momentum flux is:

$$\tau_x = -\mu_p \frac{\partial v_x}{\partial z} - \frac{\partial \gamma}{\partial T}\frac{\partial T}{\partial x}$$

$$\tau_y = -\mu_p \frac{\partial v_y}{\partial z} - \frac{\partial \gamma}{\partial T}\frac{\partial T}{\partial y}$$

The first term in each equation represents plasma drag force, while the second term denotes shear stress due to surface tension gradient (Marangoni shear stress). For AA-TIG welding of SUS304 stainless steel [2,3], oxygen in the shielding atmosphere alters surface tension. Experimental measurements of weld pool surface tension variation with temperature yielded $\partial\gamma/\partial T$ values of -0.0143 and 3.46 mN/(m·K) for pure Ar and Ar+O_2 shielding, respectively [40]. Nitrogen-oxygen analysis also showed that weld oxygen content changed from 0.0036% under pure Ar shielding to 0.016% with oxygen addition. Sahoo et al. [41] derived a semi-empirical relationship for liquid Fe surface tension γ as a function of active element activity f and temperature T under equilibrium conditions: $\gamma = \gamma(T, f)$. This relationship shows that at higher oxygen content, surface tension in the lower temperature range decreases with increasing temperature. The weld oxygen content was not substituted into this relationship in the calculations because weld oxygen content differs from oxygen content in the weld pool, especially at the surface. Dissolved oxygen decreases significantly during cooling, while only surface oxygen affects surface tension. Using weld oxygen content would likely underestimate oxygen' s effect on surface tension. Furthermore, studies show that oxygen in the weld pool during welding accumulates at the surface with non-uniform distribution [42], requiring consideration of surface tension variation due to concentration gradients, i.e., $(\partial\gamma/\partial r)$, where r represents position, to obtain the complete differential expression for surface tension gradient. These aspects will be explored in future research. However, for sulfur, the situation is less complex than for oxygen, allowing weld sulfur content to be used in calculations [10,11,20] or even determining sulfur distribution in the weld pool [11].

1.3 Numerical Method

Structured and unstructured grids were generated using Gambit software with hexahedral elements, with refined grids near the cathode and anode. The model was solved using FLUENT software, with electromagnetic variables solved through additional user-defined scalar (UDS) equations and source terms and boundary conditions incorporated via user-defined functions (UDFs). The SIMPLE algorithm was employed for solving the equation system, with second-order upwind discretization to ensure computational accuracy. The convergence criterion for the energy equation was 10^{-6}, while 10^{-3} was used for other equations.

Material properties used in the calculations were compiled from references [19, 25, 43], and physical constants are listed in .

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2 Simulation Results and Discussion

The welding conditions used in the simulation were: current (100+100) A, Ar gas flow rate 25 L/min, tungsten electrode spacing 3 mm, arc length 3 mm, SUS304 stainless steel base metal with 8 mm thickness, and spot welding for 2 s. Computational results for both Ar shielding ($\partial\gamma/\partial T < 0$) and Ar+$O_2$ shielding ($\partial\gamma/\partial T > 0$) are compared and discussed.

2.1 Simulation Results

[Figure 2: see original paper] shows the temperature and flow fields in the xz and yz planes of the arc and weld pool after 2 s of spot welding. [Figure 2: see original paper]a shows results for pure Ar shielding, while [Figure 2: see original paper]b shows results for Ar+O_2 shielding. Due to the close proximity of the two tungsten electrodes, the arcs couple into a single entity under electromagnetic forces, with a maximum arc temperature of 15645 K. Comparison between [Figure 2: see original paper]a and b reveals that the double tungsten electrode arc shape is no longer rotationally symmetric but expands relatively in the yz plane, differing from conventional TIG arcs. This occurs because the two plasma flows move toward each other under electromagnetic forces at velocities exceeding 80 m/s, mutually obstructing each other and forcing expansion in the y-direction. This enhanced flow in the y-direction transports arc heat along this direction, causing the arc to expand more in y than in x. [Figure 2: see original paper]a also shows plasma flows attracting each other and even moving upward near the inner sides of the tungsten electrodes, causing upward expansion of the arc column, which is more evident in [Figure 2: see original paper]b. These findings are consistent with experimental studies [6,7] and other simulation results [26].

Under Ar shielding, an outward circulation forms in the weld pool with a maximum velocity of approximately 0.11 m/s. As shown by the 1673 K isotherm, the weld pool is relatively wide and shallow with a depth of about 1.2 mm, and the maximum weld pool temperature is approximately 2303 K ([Figure 2: see original paper]a and b). Under Ar+O_2 shielding ([Figure 2: see original paper]c and d), weld pool flow is from outside to inside with a maximum velocity of about 0.41 m/s, indicating more intense flow. Compared with Ar shielding, the weld pool isotherms contract, the maximum surface temperature increases to over 2400 K due to inward flow transferring arc heat to the center, and penetration increases significantly to over 4 mm while the width contracts slightly. However, these weld pool changes have minimal effect on the arc. Similar to the arc shape, the weld pool profile also exhibits asymmetry with slight expansion in the yz plane.

[Figure 3: see original paper] shows the current density and electromagnetic force distributions in the xz plane. As seen in [Figure 3: see original paper]a, current lines concentrate near the tungsten electrode tips where current density is maximum, producing maximum Joule heating near the cathode. However, the high-temperature region does not simply form near the cathode tips but appears in the arc column center, thanks to plasma flow transporting arc heat toward the center. At the anode surface, current lines contract slightly due to decreased electrical conductivity from lower temperatures near the anode, narrowing the conduction channel. In the anode, however, current spreads more because of high and relatively temperature-independent conductivity.

[Figure 3: see original paper]b shows that electromagnetic forces point inward and downward, driving the plasma flow patterns shown in [Figure 2: see original paper]. Electromagnetic forces are maximum at the cathode tips where current density is highest, producing large magnetic flux density and consequently large electromagnetic force. In contrast, electromagnetic forces in the anode are much smaller than in the arc region due to significantly lower current density.

[Figure 4: see original paper] shows the temperature field, flow field, and magnetic field at 0.15 mm above the anode surface. Although this location is close to the weld pool, its temperature is much higher than the weld pool, creating a large temperature gradient that drives substantial arc heat into the anode. Plasma flow velocity is small compared to the high-temperature arc column region but remains large relative to the weld pool flow (on the order of 0.1 m/s), creating a significant velocity gradient that generates plasma drag force driving outward weld pool flow. The arc expansion characteristic in the y-direction is also clearly reflected. [Figure 4: see original paper]b shows that magnetic flux density at 0.15 mm above the anode reaches the order of 10^{-2} T, forming a unified field that peaks near the center point.

[Figure 5: see original paper]a and b show the temperature fields at the weld pool surface under pure Ar and Ar+O_2 shielding, respectively. To more clearly represent temperature variations, distributions in the x and y directions are plotted in [Figure 6: see original paper]. [Figure 5: see original paper]a and b reveal that the weld pool surface temperature field contracts significantly under Ar+O_2 shielding compared to pure Ar shielding, with reduced high-temperature regions, also evident in [Figure 6: see original paper]. [Figure 6: see original paper] further shows that weld pool surface temperatures increase under Ar+O_2 shielding, with peak temperatures approaching 2500 K. Additionally, both shielding conditions show weld pool profiles elongated in the y-direction ($W_y > W_x$), as indicated by the 1673 K isotherm. This is clearly visible in [Figure 6: see original paper], where temperature distributions in the x and y directions do not coincide, showing slight expansion in the y-direction.

2.2 Weld Pool Heat Transfer and Flow Analysis

[Figure 7: see original paper]a and b show the current density and heat flux distributions in the x and y directions at the weld pool surface under Ar+O_2 shielding. The current density and heat flux expand more in the y-direction, causing the weld pool expansion in y. As shown in equation (19), heat flux consists of three components: q_e, q_c, and q_r. [Figure 7: see original paper] shows maximum current density of 7.85×10^6 A/m^2, q_e peak of 2.54×10^7 W/m^2, q_c peak of 3.65×10^7 W/m^2, and q_a peak of 6.12×10^7 W/m^2. In the anode center region, $q_e > q_c$, while in the edge region, $q_e < q_c$, indicating that electron absorption dominates heat transfer in the center while conduction dominates at the edges. This occurs because arc temperature drops sharply in edge regions, causing electrical conductivity to decrease rapidly and current density to diminish significantly, reducing q_e. However, the temperature gradient between arc and weld pool remains large, making heat conduction significant.

Integrating each heat flux component over the anode surface yields:

$$Q_a = \int_\Omega q_a \, ds = \int_\Omega q_e \, ds + \int_\Omega q_c \, ds + \int_\Omega q_r \, ds = 930 + 653 - 23 = 1560 \text{ W}$$

where Q_a, Q_e, Q_c, and Q_r represent total anode heat input, heat input due to electron absorption, heat input due to conduction, and heat loss due to radiation, respectively; Ω is the integration region (anode surface); and ds is the differential area element. The results show that Q_e accounts for 58.7% of total anode heat input Q_a, representing the dominant component, while Q_c contributes 41.3% as a secondary component. These findings are consistent with studies on TIG arc welding by Tanaka et al. [19], Yin et al. [24], and Wu et al. [44]. Using the calculated voltage of 8.6 V and total current of 200 A, the arc thermal efficiency is determined to be 90.7%. This value is higher than actual thermal efficiency because the model simplifies the electrode regions, particularly the cathode region, neglecting cathode voltage drop and resulting in lower calculated arc voltage and consequently higher thermal efficiency.

For pure Ar shielding ($\partial\gamma/\partial T < 0$), changes in weld pool surface temperature cause minimal variations in Q_c and Q_r, while Q_e remains unchanged. The heat flux distribution is nearly identical to that under Ar+O_2 shielding. Similarly, total anode heat input under pure Ar shielding is:

$$Q_a = \int_\Omega q_e \, ds + \int_\Omega q_c \, ds + \int_\Omega q_r \, ds = 930 + 624.3 - 46.5 = 1508.5 \text{ W}$$

Thus, anode heat input remains nearly unchanged compared to Ar+O_2 shielding, with minor changes in Q_c and Q_r due to variations in weld pool surface temperature.

[Figure 8: see original paper] shows the shear stress distributions at the weld pool surface under Ar and Ar+O_2 shielding, where τ_p is plasma drag force and τ_M is Marangoni shear stress due to surface tension gradient. The results show that τ_p is nearly identical in both cases. Under pure Ar shielding, Marangoni shear stress and plasma drag force have the same direction and similar magnitude, driving weld pool flow from inside to outside. The larger shear stress in the y-direction explains the greater weld width in that direction ([Figure 8: see original paper]a and b). Under Ar+O_2 shielding, τ_p and τ_M have opposite directions, but the magnitude under Ar+O_2 shielding is much greater than under Ar shielding. Consequently, the total shear stress at the weld pool surface is determined by Marangoni shear stress, as shown by $\tau_p + \tau_M$ in the figure, resulting in a direction opposite to that under pure Ar shielding ([Figure 8: see original paper]c and d). This occurs because oxygen, as a surface-active element, changes the temperature coefficient of surface tension, causing stainless steel weld pool surface tension to increase with temperature (positive temperature coefficient) and reversing the direction of τ_M, ultimately leading to significantly increased penetration and slightly contracted width. Notably, the plasma drag force τ_p is much smaller than that of TIG arcs under equivalent conditions [19,20,23,25], which benefits penetration increase. Additionally, shear stress in the y-direction is greater than in the x-direction. Since τ_M is generated in the weld pool region, which is elongated in the y-direction, τ_M correspondingly expands.

[Figure 9: see original paper] shows weld pool flow under individual driving forces after 2 s for both Ar shielding ($\partial\gamma/\partial T < 0$) and Ar+$O_2$ shielding ($\partial\gamma/\partial T > 0$). Regardless of which force acts alone, the resulting weld width is greater in the y-direction than in the x-direction, caused by heat flux distribution at the weld pool surface. Buoyancy, plasma drag force, and negative surface tension temperature coefficient individually drive flow from inside to outside, producing shallow depth and large width. Electromagnetic force and positive surface tension temperature coefficient individually drive flow from outside to inside, producing deep penetration and small width. [Figure 10: see original paper] shows weld pool dimensions and maximum velocities under individual driving forces. Penetration D exceeds 3 mm under positive surface tension temperature coefficient and electromagnetic force, but is less than 2 mm under buoyancy, plasma drag force, and negative surface tension temperature coefficient. The ranking of penetration under individual forces is: positive surface tension temperature coefficient > electromagnetic force > buoyancy > negative surface tension temperature coefficient > plasma drag force, with width showing the opposite trend. Maximum weld pool velocities reach 0.26 m/s under $\partial\gamma/\partial T > 0$ (strongest effect) and 0.09 m/s under $\partial\gamma/\partial T < 0$. Electromagnetic force and plasma drag force produce velocities of 0.088 and 0.085 m/s, respectively, showing similar magnitudes but opposite effects. Buoyancy has the weakest effect with maximum velocity of about 0.03 m/s. Notably, plasma drag force-driven flow is significantly smaller than the 0.47 m/s observed in TIG welding [19], decreasing by an order of magnitude due to reduced plasma drag force. This suggests the ranking of weld pool driving forces is: surface tension > electromagnetic force > plasma drag force > buoyancy. Nevertheless, generating flow on the order of 10^2 m/s in a 10 mm weld pool demonstrates that convection within the weld pool is extremely intense.

As previously analyzed, higher weld pool velocity leads to larger weld pool dimensions (depth D or width W). The altered flow under Ar+O_2 shielding significantly increases penetration while slightly contracting width. However, the melting process is fundamentally thermal in nature; flow changes alone cannot directly alter weld pool geometry—rather, heat convection induced by flow has a direct effect. In addition to intense metal flow-driven convection, significant heat conduction and relatively small thermal radiation also exist. Under both Ar and Ar+O_2 shielding, total arc heat input remains nearly unchanged, yet weld pool geometries differ dramatically. The increased penetration is not caused by heat input changes, and explanation based solely on flow changes is insufficient. Previous studies on TIG and A-TIG weld pool geometries [2,11,12,18–23,25] focused primarily on metal flow effects, rarely addressing the relative importance of convective versus conductive heat transfer. Convective and conductive heat transfer have fundamentally different origins: the former arises from fluid temperature gradients, while the latter results from fluid motion, corresponding to the diffusion term ($\nabla \cdot (k\nabla T)$) and convection term ($\nabla \cdot (\rho c_P \mathbf{v} T)$) in equation (6), respectively.

To determine the relative magnitudes of convective and conductive heat transfer in the weld pool, the dimensionless Peclet number (Pe) is introduced [8,13]:

$$\mathrm{Pe} = \frac{u\rho c_P \Delta T / L_R}{k \Delta T / L_R^2} = \frac{u \rho c_P L_R}{k}$$

where u is velocity, L_R is the characteristic weld pool length (taken as the upper surface radius), and k is thermal conductivity. Based on our results, for pure Ar shielding ($\partial\gamma/\partial T < 0$), using $u = 0.1$ m/s, $\rho = 7000$ kg/m^3, $c_P = 600$ J/(kg·K), $L_R = 0.004$ m, and $k = 20$ W/(m·K), we obtain Pe = 76. For Ar+O_2 shielding ($\partial\gamma/\partial T > 0$), using $u = 0.2$ m/s, $\rho = 7000$ kg/m^3, $c_P = 600$ J/(kg·K), $L_R = 0.003$ m, and $k = 20$ W/(m·K), we obtain Pe = 115. In both cases, Pe » 1, indicating that convective heat transfer dominates, with stronger convection under $\partial\gamma/\partial T > 0$. This explains the isotherm changes shown in [Figure 2: see original paper] and [Figure 10: see original paper]. Therefore, under Ar shielding, surface tension-dominated flow drives metal from inside to outside, creating outward convection. Due to the dominant convective heat transfer, most arc heat is transferred to the weld pool edges, melting base metal and forming a wide, shallow geometry. Under Ar+O_2 shielding, surface tension-dominated flow creates counterclockwise convection. With convective heat transfer dominating, most arc heat is transferred to the weld pool center, melting base metal and forming a deep, narrow geometry.

Without dominant convective heat transfer, the weld pool under Ar shielding might not be as shallow, and under Ar+O_2 shielding might not be as deep (several times deeper). It can be inferred that for materials with higher thermal conductivity such as aluminum alloys, convective dominance would be less pronounced than for stainless steel, while conductive heat transfer would increase and might even dominate under certain conditions, as demonstrated by Rai et al. [45] in studies of laser keyhole welding of different materials.

[Figure 11: see original paper] shows the temporal evolution of weld pool dimensions and heat input under Ar+O_2 shielding. [Figure 11: see original paper]a indicates that both penetration and width increase with welding time, with penetration increasing more rapidly as arc heat is transferred to the weld pool center. [Figure 11: see original paper]b shows that peak heat flux and current density at the weld pool surface decrease slightly over time: current density peak decreases by 5.8×10^4 A/m^2 and heat flux peak by 4×10^4 W/m^2. As weld pool temperature continuously increases, anode surface electrical conductivity increases, expanding the current channel and reducing current density peak. Meanwhile, increased weld pool surface temperature reduces the temperature gradient between arc and weld pool, decreasing conductive heat. The combined effect reduces heat flux. In the calculations, arc temperature decreased by only 7 K over time, with minimal effects on plasma flow velocity and arc voltage.

2.3 Experimental Validation

[Figure 12: see original paper] and [Figure 13: see original paper] compare simulated and experimental arc shapes and weld pool geometries. [Figure 12: see original paper] shows that the double electrode TIG arc expands relatively in the yz plane, consistent with simulation results. [Figure 13: see original paper] demonstrates good agreement between simulated and experimental weld pool profiles. [Figure 14: see original paper] compares simulated and experimental weld dimensions. Both experiments and calculations show $W_y > W_x$, but this trend is more pronounced under pure Ar shielding ($\partial\gamma/\partial T < 0$). Under Ar+$O_2$ shielding ($\partial\gamma/\partial T > 0$), flow changes reduce the difference between W_y and W_x. For penetration, experimental results show about 1.5 mm under Ar shielding ($\partial\gamma/\partial T < 0$) versus 1.2 mm simulated, while under Ar+O_2 shielding ($\partial\gamma/\partial T > 0$), simulated penetration is slightly less than experimental values. Experimental results show W_x is 0.8 mm smaller than W_y, while simulation shows 1.1 mm difference. Overall, simulation results accurately reflect actual conditions.

The weld pool profiles suggest that simulated anode heat input and heat flux distribution radii are slightly smaller than experimental values, primarily due to simplified treatment of the anode region. Only the main components of anode heat transfer were considered. In reality, the arc cathode and anode sheath regions deviate from LTE, and heat transfer processes [19,20,38,46] are far more complex than described in this model. Anode metal vapor generation also significantly affects the arc [20]. Additionally, differences between material properties used in the model and actual materials affect results [10], and minor deformation of the free surface influences the arc and heat transfer to the anode

[14].

It should be noted that under current welding conditions, these non-axisymmetric characteristics all show expansion along the y-direction. However, it can be inferred that as welding parameters change, particularly with increased tungsten electrode spacing, the non-axisymmetry would extend along the x-direction. These aspects will be investigated in future studies.

Conclusions

1. The non-axisymmetric characteristics of the double electrode arc lead to non-axisymmetric distributions of current density, heat flux, plasma drag force, and Marangoni shear stress at the weld pool surface. These distributions expand in the direction perpendicular to the electrode arrangement, ultimately determining the non-axisymmetric weld pool geometry.
2. The primary driving force in double electrode TIG weld pools is Marangoni shear stress. Electromagnetic force and plasma drag force have comparable magnitudes but drive flow in opposite directions, while buoyancy has the smallest effect. The plasma drag force is significantly smaller than in TIG arcs.
3. Non-axisymmetric heat flux and Marangoni shear stress distributions determine the non-axisymmetric weld pool geometry.
4. The reversal of Marangoni shear stress direction and the dominant role of convective heat transfer collectively cause increased penetration and slightly contracted width under $Ar+O_2$ shielding.
5. The continuous increase in weld pool temperature leads to slight decreases in peak heat flux at the weld pool surface, with minimal effect on arc behavior.

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Note: Figure translations are in progress. See original paper for figures.