EFFECT OF SURFACE TENSION COEFFICIENT SIGN ON TURBULENT CONVECTION IN A GAS TUNGSTEN ARC WELDING (GTAW) PROCESS

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Abstract: - The effects of the sign of the surface tension coefficient on turbulent weld pool convection are studied for a typical Gas Tungsten Arc Welding (GTAW) process. Three-dimensional turbulent weld-pool convection in a pool is simulated using a suitably modified high Reynolds number \( \varepsilon \)-\( k \) model in order to account for the morphology of solid-liquid interface and the phase change aspect of the problem. Key effects regarding the sign of surface tension coefficient in the turbulent transport in a GTAW process are highlighted by comparing the turbulent simulation results with the corresponding ones from a laminar model, keeping all process parameters unaltered.

Key-Words: - Surface tension coefficient, Weld pool convection, Turbulent convection, \( k-\varepsilon \) model

1 Introduction

Arc welding is one of the most popular joining techniques, in which the workpieces are locally melted by an intense heat source. The local melting is followed by a subsequent resolidification when the heat source moves away from the previously molten zone. The microstructural constitution and the extent of Heat Affected Zone (HAZ) of the arc welded joint thus formed are strongly dependent on the thermal history involving melting and subsequent solidification. The mechanical properties of the joint, in turn, are governed by the microstructure. That is why it is of immense importance to have a thorough knowledge of the momentum and heat transport in the molten pool. However, in practical cases, due to extreme small size of the weld pool, opacity of the molten metal and light radiation from electric arc, experimental studies regarding flow visualization inside the weld pools are virtually ruled out. Therefore, it is extremely important to develop appropriate mathematical models for weld pool convection, in order to understand fundamentals of weld pool transport, both qualitatively and quantitatively.

A vast body of literature is available on weld pool transport containing several mathematical models of relevance [1-10]. The above models have contributed significantly towards the weld pool transport in a lot better manner than the era when welding performance was standardized based on empirical methods. Several numerical models are proposed with various degree of complexity by a number of researchers for studying of weld pool fluid flow and heat transfer for a gas tungsten arc welding (GTAW) process [1-7]. Recently, Chao and Szekely [8] have modeled stationary turbulent GTAW weld pools using \( k-\varepsilon \) models. However, to date, the treatment of dynamic evolution solid-liquid interface by proper wall conditions with the simultaneous solution of primitive variables has rarely been addressed, in the context of turbulent transport in a GTAW process. Chakraborty et al. [9], for the first time, have proposed a two-dimensional turbulence model for a GTAW process simulation, in which the dynamic evolution of irregular morphology of the solid-liquid interface has been addressed by employing modified wall co conditions for solid liquid phase boundaries in the framework of a standard \( k-\varepsilon \) model. Recently, Chakraborty et al. [10] have extended the above model successfully to analyze three-dimensional turbulent transport processes in GTAW process.

It is important to recognize that turbulent transport processes in arc welding are further complicated by the fact that that there is a very significant role played by the nature of surface tension forces on account of thermal gradients existing in the weld pool, effects of which, in certain cases, may be rather intuitive, but by no means obvious. It can be noted here that, in the context of
surface tension driven flows, such convection is termed as ‘Marangoni convection’. Particularly in case of metals, surface tension forces are strong functions of temperature. Further, they are strong functions of weld pool constitutive elements as well. For instance, if surface-active elements like sulphur and manganese are present in steel, then the surface tension increases with an increase in temperature, indicating a positive surface tension coefficient \( \sigma \). In absence of surface-active elements, on the other hand, the surface tension coefficient for steel is negative. In arc welding processes, the large temperature variation on the top surface of the molten pool results in a considerable surface tension differential, which in turn, initiates the convection process in the molten pool. Since this convection governs the temperature distribution and weld pool morphology, including weld pool shape and the penetration, it is extremely important to investigate the effects of sign of surface tension coefficient on weld pool transport. Accordingly, aim of the present work is to study the effect of surface tension coefficient with regard to turbulent weldpool transport, and to emphasize the corresponding distinctive features from predictions made using equivalent laminar models commonly employed in the literature. Conclusive remarks are made on the differences in nature of overall weldpool transport and evolution for the cases studied.

2 Problem Formulation

In the GTAW welding process, the heat source in the form of electrical arc is created between a non-consumable tungsten electrode and the work-piece in an inert gas atmosphere. The arc power received by the work-piece is specified by arc efficiency, \( \eta \). The available heat to the workpiece is related to the arc power input by \( q = \eta Q = \eta VI \), where \( V \) and \( I \) are specified arc voltage and arc current, respectively. As the work-piece melts, the surface tension differential at the top surface initiates the fluid flow, which is responsible for the transport of the fluid momentum, thermal energy, turbulent kinetic energy, and dissipation of turbulent kinetic energy inside the molten pool. The electromagnetic body force (Lorentz) and buoyancy forces also take an important part in the transport process, but in most cases their strengths are about an order of magnitude lower than surface tension (Marangoni) forces [12]. In the case of turbulent weld pools, both convection and diffusion mechanisms are of significant importance, and relative strength of these two mechanisms determines the nature of transport phenomena in the pool as well as its morphology including shape and size.

The transport phenomena occurring inside the weld pool can be conveniently studied with respect to a coordinate system that translates with the arc heat source. Accordingly, we introduce the following coordinate transformation:

\[
x = x^* - (-U_{scan})t
\]

where \( U_{scan} \) is the speed of welding torch and \( x, y, z \) are coordinates in a frame moving with the torch. It is important to recognize in equation (1) the electrode scanning speed \( U_{scan} \) is assumed to be in the negative \( x \) direction which is accounted for by the negative sign in front of \( U_{scan} \) in equation (1). Henceforth, we will follow the following tensorial notation for description of conservation equation

\[
x_1 = x, x_2 = y, x_3 = z
\]

Carrying out the transformation described in equation (2) and noting that the scanning is done in negative \( x^* \) direction, we obtain:

\[
\frac{\partial (\rho \phi)}{\partial t} + \frac{\partial (\rho U_i \phi)}{\partial x_i} = \frac{\partial}{\partial x_i} \left( \frac{\partial \phi}{\partial x_i} \right) + S - \frac{\partial (\rho U_i \phi)}{\partial x_i} \frac{\partial (\rho U_i \phi)}{\partial x_i} \frac{\partial (\rho U_i \phi)}{\partial x_i}
\]

For different \( \phi \)'s, the governing equations in the moving coordinate system assume the following forms in tensorial notation where \( \bar{\phi} \) and \( \phi' \) corresponds to the mean and fluctuating component of the primitive variable \( \phi \) and \( \hat{\phi} \) is the so called Reynolds flux arising out of the turbulent fluctuations. Mass conservation equation for incompressible flow is given by:

\[
\frac{\partial \rho U_i}{\partial x_i} = 0, \quad \frac{\partial \rho U_i}{\partial x_i} = 0
\]

In the present study the density variation due to temperature change is taken into account by Boussinesq’s approximation. Under this assumption the momentum equations are given by:

\[
\frac{\partial (\rho U_i)}{\partial t} + \rho U_i \frac{\partial U_i}{\partial x_j} = -\frac{\partial p}{\partial x_i} + (J \cdot \vec{B})_i + \frac{\partial}{\partial x_j} \left( \left( \frac{\partial U_i}{\partial x_j} \right) \frac{\partial \rho U_i}{\partial x_j} \right) - \frac{\partial (\rho U_i \phi)}{\partial x_i}
\]

where \( g_i \) is the acceleration component due to gravity in the \( i \)’th direction, and \( T_{ref} \) is the reference temperature which is taken to be equal to the melting temperature, \( T_m \). The source term \( \vec{S}_l \) in equation (5) originates from the consideration that the morphology of the phase changing part between completely solid and liquid phase can be treated as an equivalent porous medium that offers resistance towards fluid
flow in that region. In a single-domain fixed-grid enthalpy-porosity formulation, this resistance can be conveniently formulated using Darcy’s model for porous media in conjunction with the Cozeny-Karman relationship as done by Brent et al. [11]. In equation (6), \( \bar{S}_t \) is given as:
\[
\bar{S}_t = -C \left( \frac{\bar{f}_t}{f_t} \right)^3 \frac{\partial^2 \bar{T}}{\partial x_i^2}
\] (6)
where the liquid fraction \( f_t = \Delta H / L \) in a given control volume is taken to be the effective porosity, with \( \Delta H \) being the latent enthalpy content of a control volume and \( L \) being the latent heat of fusion. In equation (6a), \( C \) is a large number \((-10^3\) and \( b \) is a small number \((-10^3\) to avoid division by zero used purely from computational consideration. This formulation ensures that the velocity undergoes a smooth transition from a zero value in completely solid phase to a finite value in the fully liquid phase. The details of formulation of the above term can be found in Brent et al. [11]. The term \(-\rho u_i u'_j\) is the Reynolds stress term, the modelling of which is done with the help of \( k - \varepsilon \) model. The Lorentz force term \( J \times B \) is evaluated by solving Maxwell’s equations following the procedure presented by Dutta et al. [5].

The single-phase energy transport equation for turbulent flow is given by:
\[
\frac{\partial (\rho \bar{T})}{\partial t} + \frac{\partial (\rho \bar{U}_i \bar{T})}{\partial x_i} = \frac{\partial}{\partial x_i} \left( \frac{K}{c} \frac{\partial \bar{T}}{\partial x_i} \right) - \frac{1}{c} \frac{\partial (\rho \Delta H)}{\partial t}
\] (7)
where \( c \) is the specific heat, \( K \) is the thermal conductivity of the material, and \( \Delta H \) is the latent enthalpy content of the computational cell under consideration. In the present analysis, the material is assumed to behave as a pure metal, and hence the phase change is isothermal, for which the latent heat content \( \Delta H = L \) when \( \bar{T} > T_m \) and \( \Delta H = 0 \) when \( \bar{T} < T_m \).

2.1 Modelling of Reynolds stress \((-\rho u_i u'_j\))

In the present analysis, we model the Reynolds stress terms appearing in equation (6) by assuming a turbulent viscosity of the form:
\[
\mu_t = \frac{1}{3} \frac{\partial^2 \bar{T}}{\partial x_i^2} k
\] (8)
where the eddy viscosity is given by
\[
\mu_t = \sqrt{f_t} C_\mu \rho k^2 \varepsilon
\] (9)
In equation (9), \( C_\mu \) is a constant, whose value is taken to be 0.09. In original form of standard \( k \)-\( \varepsilon \) model the term \( \sqrt{f_t} \) does not appear in the expression of eddy viscosity. This extra factor is introduced following Shyy et al. [13], to take into account smooth transition of eddy viscosity from zero value to the value predicted by standard phase \( k \)-\( \varepsilon \) model across the phase changing interface. Following the same closure technique used for the Reynolds stresses, the turbulent heat fluxes (Reynolds heat fluxes) appearing in equation (7) can be written as:
\[
-\frac{\bar{u}_i \bar{H}'}{c} = -\bar{u}_i \bar{T} = \alpha_t \frac{\partial \bar{T}}{\partial x_i}
\] (10)
where \( \alpha_t \) is the eddy thermal diffusivity. From the analogy of laminar flow, \( \alpha_t \) can be expressed as:
\[
\alpha_t = \frac{\mu_t}{\rho \sigma_t}
\] (11)
where \( \sigma_t \) is turbulent Prandtl number with a value \( \sigma_t \approx 0.9 \). The eddy diffusivity value is evaluated using the turbulent kinetic energy \( k \) and dissipation \( \varepsilon \) obtained from the solution of the transport equations of \( k \) and \( \varepsilon \). The transport equations of \( k \) and \( \varepsilon \) are given by:

**k equation**
\[
\rho \frac{\partial k}{\partial t} + \rho \bar{U}_i \frac{\partial k}{\partial x_i} = \frac{\partial}{\partial x_i} \left( \frac{\mu + \mu_t}{\sigma_t} \frac{\partial k}{\partial x_i} \right) \] (12)
\[
+ \mu_t \left( \frac{\partial \bar{U}_i}{\partial x_i} \right) \frac{\partial \bar{U}_i}{\partial x_i} - \frac{\mu_t}{\sigma_t} S_{\text{diff}} - \rho \bar{U}_i \frac{\partial k}{\partial x_i}
\]

**\( \varepsilon \) equation**
\[
\rho \frac{\partial \varepsilon}{\partial t} + \rho \bar{U}_i \frac{\partial \varepsilon}{\partial x_i} = \frac{\partial}{\partial x_i} \left( \frac{\mu + \mu_t}{\sigma_t} \frac{\partial \varepsilon}{\partial x_i} \right)
\] (13)
\[
- C_{\varepsilon 2} \varepsilon^2 \rho - \rho \bar{U}_i \frac{\partial \varepsilon}{\partial x_i}
\]
\[
+ C_{\varepsilon 1} \mu_t \left( \frac{\partial \bar{U}_i}{\partial x_i} \right) \frac{\partial \bar{U}_i}{\partial x_i} \frac{\varepsilon}{k} - C_{\varepsilon 2} \mu_t \left( \frac{\partial \bar{U}_i}{\partial x_i} \right) \frac{\varepsilon}{k}
\]
where \( C_\mu = 0.09, C_{\varepsilon 1} = 1.44, C_{\varepsilon 2} = 1.92, \sigma_k = 1.0 \) and \( \sigma_\varepsilon = 1.3 \).

2.2 Boundary Conditions

2.2.1 Top Surface

When At the top surface, the incident heat flux is considered with a Gaussian distribution, along with
convective and radiative heat losses from the workpiece (y being the vertical direction):
\[-K \frac{\partial T}{\partial y} = -q^*(r) + h(T - T_w) + \varepsilon, \sigma_{rad}(T^4 - T_w^4)\]
(14)

where $q^*(r)$ is the net arc heat flux distributed in a Gaussian manner with a radius of $r_q$, $T$ is the radius of the arc, $\varepsilon$ is the emissivity of the top surface, $T_w$ is the ambient temperature and $\sigma_{rad}$ is the Stefan-Boltzmann constant. From the free surface shear balance between viscous force and surface tension:
\[
\mu \frac{\partial U}{\partial y} = \frac{\partial \sigma_{sw}}{\partial x}; \mu \frac{\partial W}{\partial y} = \frac{\partial \sigma_{sw}}{\partial T} \frac{\partial \sigma}{\partial z}\]
(15)

where $\sigma_{sw}$ is the surface tension. It is to be noted here the algebraic sign of the surface tension coefficient $\frac{\partial \sigma_{sw}}{\partial T}$ needs to be taken into account for equation (15). Further, for the a flat free top surface [5, 14]:
\[
\nabla = 0 \frac{\partial k}{\partial y} = 0 \frac{\partial \varepsilon}{\partial y} = 0 \]
(16)

2.2.2 Bottom and Side Surfaces
The bottom face is insulated
\[
\frac{\partial T}{\partial y} = 0\]
(17)
while the four side faces are subjected to convective heat transfer boundary condition:
\[-K \frac{\partial T}{\partial n} = h(T - T_w)\]
(18)

where $n$ is the outward normal direction of surface concerned.

2.2.3 Solid/Liquid Interface
It is apparent that the solid/liquid interface in this problem acts as a wall. According to the enthalpy-porosity formulation, it is not needed to track the interface explicitly, since the interface comes out as a natural outcome of the solution procedure itself. However, the evolving interface locations are important from the point of solutions for $k$ and $\varepsilon$ equations, since the values of $k$ and $\varepsilon$ are to be specified for near wall points with the help of standard wall functions [15] used for high Reynolds number $k - \varepsilon$ model, which satisfies the conditions $k = 0$ and $\frac{\partial \varepsilon}{\partial n} = 0$ at the solid/liquid interface where $n$ is the local outward normal direction at the phase changing interface.

### 3 Numerical Implementation

The coupled conservation equations are solved simultaneously using a pressure-based time-implicit finite volume technique according to the SIMPLER algorithm [16]. The size of the computational domain is taken to be as 50mm×15mm×40mm. For the purpose of the present investigation, numerical simulations are performed for the case of a typical GTA welding situation with steel as the base material, the thermo-physical properties and processing parameters for which are listed in Table 1.

<table>
<thead>
<tr>
<th>PROBLEM DATA</th>
<th>VALUE</th>
</tr>
</thead>
<tbody>
<tr>
<td>Arc power ($Q$)</td>
<td>360W</td>
</tr>
<tr>
<td>Laser efficiency ($\eta$)</td>
<td>90%</td>
</tr>
<tr>
<td>Scanning speed ($U_{scan}$)</td>
<td>8.89mm/s</td>
</tr>
<tr>
<td>Torch radius ($r_q$)</td>
<td>3.5mm</td>
</tr>
</tbody>
</table>

Table 1: Table of process parameters

For the positive surface tension coefficient case, the value of surface tension coefficient is taken to be $\frac{\partial \sigma_{sw}}{\partial T} = 5 \times 10^{-4} N/m.K$. For the negative surface tension coefficient case studies, the surface tension coefficient is taken as $\frac{\partial \sigma_{sw}}{\partial T} = -5 \times 10^{-4} N/m.K$. For other thermo-physical properties, one may refer to Ref. [5] and [10].

3.1 Choice of Grid Size

For the present simulation the choice of grid size is determined by two criteria, as follows:

3.1.1 Criterion 1
In order to resolve the flow within the surface tension driven boundary layer, at least a few (typically five) grid points are accommodated inside it.

3.1.2 Criterion 2
In order to specify the correct eddy diffusivity values near the wall, it is necessary to control the grid size in its vicinity. If the grid points immediately adjacent to the wall fall beyond the ‘near wall’ regime but the diffusion coefficients are still evaluated by log-law, the simulation may lead to an erroneous estimation of the eddy diffusion coefficients. In the present case, however, the wall position and geometry change with time-step and iteration. Hence, it is necessary to have a grid distribution such that the grid adjacent to the wall
always falls within the logarithmic layer. This condition is mathematically given as:

\[ 11.5 < x^* = \frac{\rho C_p^{0.25} h^{0.5} x}{\mu} \leq 50 \]  

(19)

From the two requirements discussed above it is evident that the grid spacing in y direction is governed by criterion 1, and that in the x and z direction are determined by criterion 2. Accordingly, the size of the topmost grids is chosen to be \(2.7 \times 10^{-7} \text{m}\). Next to this in y direction, the grid spacing is taken as \(1.35 \times 10^{-6} \text{m}\), followed by a depth of \(3.5 \times 10^{-5} \text{m}\). Thereafter a uniform grid depth of \(1 \times 10^{-4} \text{m}\) is employed for most of the remaining part of the pool. Outside the molten pool, a non-uniform coarser grid is chosen. In the x-direction, an optimized grid size near the wall is found to be \(5 \times 10^{-3} \text{m}\). Away from the wall, the grid size increases gradually. Grid independence study confirms that a finer grid system does not alter the results appreciably. Based on the above findings, a \((50 \times 33 \times 29)\) grid system is used to discretise the working domain \((50 \text{mm} \times 15 \text{mm} \times 40 \text{mm})\).

### 3.2 Choice of Time Step

For the welding parameters used for the present study, initiation of melting takes place after about 0.25 s. Before melting, the heat transfer is within a conduction-dominated regime, for which a large time step (of about 0.05 s) is chosen so that melting temperature is reached within five time steps. However, once melting starts, it is observed that time step only as small as about 0.002 s lead towards monotonic convergence during this period of initial transience. Typically after about 1.5 s, the molten pool attains almost a quasi-steady state, when changes in primitive variable values between the consecutive time steps are very small. At this stage, slightly higher time steps (typically about 0.005 s) can be used safely to save the overall computational time. After 2.0 s, when the pool is sufficiently developed, time steps as high as 0.01 s could be implemented without any numerical oscillation. Finally, the computation is carried out up to 3 s to ensure that quasi-steady state has actually been attained.

### 4 Results and discussions

#### 4.1 Effect of surface tension coefficient on weld pool morphology

Temperature and velocity profiles in the top view, longitudinal view at the plane of maximum penetration, and the cross sectional view at the same plane, are presented for turbulent convections with different surface tension coefficient signs, corresponding to the same set of process parameters.

![Fig. 1: Temperature distribution for turbulent case with \(U_{\text{scan}} = 8.89 \text{ mm/s, power} = 3.6 \text{ kW, } \eta = 0.9, \text{ and } \partial \sigma_{\text{sur}} / \partial T = 0.0005 \text{ N/mK}.\) (a) Top view (b) longitudinal sectional view at the plane of maximum penetration, and (c) cross sectional view at the plane of maximum penetration. All dimensions are in meters; temperature labels are in degrees Celsius. The contour label 1500 °C represents the melting temperature of the base metal.](image)

Figures 1 and 2 are plotted for the case of a positive surface tension coefficient, as detailed in the corresponding figure captions. The directional effect due to electrode scanning along the negative x-direction is evident from the isotherms in Figs. 1a and b. It can also be seen that the maximum temperature is attained near the center of the pool. The location of maximum depth is nearly at the center of the pool, but not exactly at the center,
which can be justified from the convection pattern shown in corresponding velocity vector plots. The velocity vector plots corresponding to the above-mentioned isotherms, are presented in Figs. 2a, b and c, respectively. Since the surface tension coefficient increases with temperature, the surface tension near the center of the pool becomes stronger in comparison to the other locations in the molten pool. Accordingly, velocity vectors on the surface are directed towards the center of the pool from all directions (Fig. 2a). As the center of the pool is approached, the surface velocity increases as a consequence of an accelerating Marangoni flow. The longitudinal view of velocity vector plot (Fig. 2b) indicates the molten metal streams from the leading edge and the trailing edge approach to the maximum temperature location. Eventually, when the two streams meet, they turn in downward direction and in the process, they transport the heat through advection. This advection of heat from the hottest part of the pool alongside the thermal diffusion in the downward direction aids the effective heat transport. The weld pool depth is a resultant effect of these two mechanisms in positive surface tension coefficient flows.

Fig. 2: Velocity distribution for turbulent case with $U_{\text{scan}} = 8.89 \text{ mm/s}$, power = 3.6 kW, $\eta = 0.9$, and $\frac{\partial \sigma_{\text{sur}}}{\partial T} = 0.0005 \text{ N/mK}$. (a) Top view (b) longitudinal sectional view at the plane of maximum penetration, and (c) cross sectional view at the plane of maximum penetration. All dimensions are in meters.

![Image](attachment:image.png)

Fig. 3: Temperature distribution for turbulent case with $U_{\text{scan}} = 8.89 \text{ mm/s}$, power = 3.6 kW, $\eta = 0.9$, and $\frac{\partial \sigma_{\text{sur}}}{\partial T} = 0.0005 \text{ N/mK}$. (a) Top view (b) longitudinal sectional view at the plane of maximum penetration, and (c) cross sectional view at the plane of maximum penetration. All dimensions are in meters; temperature labels are in degrees Celsius. The contour label 1500 ºC represents the melting temperature of the base metal.

The above simulation, essentially for the same set of welding parameters and thermophysical properties, but for a negative surface tension coefficient is subsequently carried out, in order to depict the influence of the sign of surface tension coefficient on weldpool convection. Temperature distributions in the top view, the longitudinal view and the cross sectional view at the plane of maximum penetration are presented in Figs. 3a, b and c respectively. It can be seen from the longitudinal view of isotherm plot that the penetration does not vary over the most of the part in the longitudinal direction. As compared to the pool shape in case of positive surface tension coefficient flow, the pool in this case is longer in the longitudinal direction. The maximum depth is attained towards the trailing edge side of the pool. The cross sectional view at the maximum penetration location (Fig. 3c) suggests the pool is shallower compared that of in case of positive surface tension coefficient flow. The isotherms are
stretched in the opposite direction of scanning in this case as well, since \(-U_{scan} \partial T / \partial x\) term continues to work as a source term in a direction opposite to the scanning direction.

Fig. 4: Velocity distribution for turbulent case with \(U_{scan} = 8.89\) mm/s, power = 3.6 kW, \(\eta = 0.9\), and \(\partial \sigma_{sur}/\partial T = -0.0005\) N/mK. (a) Top view, (b) longitudinal sectional view at the plane of maximum penetration, and (c) cross sectional view at the plane of maximum penetration. All dimensions are in meters.

The velocity vector plots corresponding to Figs. 3a, b and c are presented in Figs. 4a, b and c respectively. In this case, as the surface tension decreases with the increase in temperature, the surface velocities are directed radially outwards as evident from Fig. 4a. The longitudinal view of the velocity vector plot (Fig. 4b) suggests that the hot fluid stream from the center of the pool accelerates as it approaches the melting front. Thus, it increases the length of the molten pool. As the fluid stream reaches the solid-liquid interface, the stagnation effect of the solid/liquid interface makes it turn in the downward direction. However, by that time most of the heat advection has already taken place. Therefore, the weld pool depth in this case is principally determined by the heat conduction mechanism. In the case of negative surface tension coefficient, the fluid from the center of the pool is accelerated as the colder region is approached. The maximum velocity is attained towards the melting front opposite to the direction of scanning, which is evident from Figure 5b. Regarding the weld pool penetration for this case, it can be noted that as the high velocity molten metal stream carries thermal energy from the center of the pool towards the trailing edge of the molten region, it has already transported most of the heat and momentum. However, when it turns downwards, it still has enough momentum and thermal energy to produce a localized increase in weld pool penetration. That is why the maximum penetration takes place towards the trailing edge of the pool in case of negative surface tension coefficient. For the same reason, the radially outward accelerating high velocity metal flow, which turns downwards near the edge of the pool, causes an enhanced localized pool penetration. This can be verified from the cross sectional view of the velocity vector plots presented in Fig. 4c.

4.2 Effects of turbulence on weld pool morphology

Comparing the isotherms obtained from turbulent simulations with the isotherms obtained from an equivalent laminar simulation (without activating turbulent transport for same set of parameters) for positive surface tension coefficient flow (Fig. 1 and Fig. 5) is evident that the maximum pool penetration takes place further away from the pool center, as compared to the laminar case. The maximum pool penetration in case of turbulent weld pool is found to be less than corresponding to a laminar weld pool. The directional asymmetry in the longitudinal section of the turbulent weld is significantly less in comparison to the directional effect can be seen from the corresponding laminar weld pool. This can be attributed to the fact that in case of turbulent flow, the effect of diffusion is much more significant than that incase of laminar convection. Since diffusion is a non-directional phenomenon, the directional asymmetry is less in turbulent weld pool. Further, in case of turbulent flow, the maximum mean temperature obtained is smaller than the maximum temperature attained in the laminar simulation, due to an enhanced mixing in the pool. Because of the lower temperature gradients, the Marangoni convection from the trailing edge of the pool is weaker in turbulent weld pool than that in case of laminar convection. Consequently, the weak stream coming from the trailing edge of the pool in turbulent case does not have enough momentum to
counter the dynamic pressure imposed by the flow from the leading edge.

Fig. 5: Temperature distribution for laminar case with $U_{scan} = 8.89$ mm/s, power = 3.6 kW, $\eta = 0.9$, and $\partial \sigma_{\text{sur}} / \partial T = 0.0005$ N/mK. (a) Top view (b) longitudinal sectional view at the plane of maximum penetration, and (c) cross sectional view at the plane of maximum penetration. All dimensions are in meters; temperature labels are in degrees Celsius. The contour label 1500 ºC represents the melting temperature of the base metal.

Comparing Fig.3 and Fig.6 it is evident that for negative surface tension coefficient flow the maximum penetration in turbulent transport is greater than that in laminar transport. The pool penetration in case of negative surface tension coefficient is governed by thermal diffusion. Therefore, in case of the turbulent convection with a negative surface tension coefficient, an enhanced diffusive effect of turbulence causes a deeper weld pool penetration. Magnitudes of the mean velocity in case of turbulent transport are smaller than the velocities in case of laminar simulations. This is also a consequence of a greater degree of momentum diffusion in turbulent weld pools. Further, in case of the turbulent transport, due to reduced mean thermal gradient resulting from turbulent diffusion, the Marangoni convection is weaker than that in laminar transport. For this reason, in a negative surface tension coefficient flow, the convection strength of the fluid emanating from the hottest portion of the pool is weaker in turbulent pool in comparison to that in laminar pool. Consequently, it takes a downward turn much earlier than that in case of laminar transport. This results in a shorter pool length and shorter pool width, in comparison to those in laminar transport.

Fig. 6: Temperature distribution for laminar case with $U_{scan} = 8.89$ mm/s, power = 3.6 kW, $\eta = 0.9$, and $\partial \sigma_{\text{sur}} / \partial T = -0.0005$ N/mK. (a) Top view (b) longitudinal sectional view at the plane of maximum penetration, and (c) cross sectional view at the plane of maximum penetration. All dimensions are in meters; temperature labels are in degrees Celsius. The contour label 1500 ºC represents the melting temperature of the base metal.

5 Conclusions
A three dimensional numerical model is employed to study the effect of sign of surface tension coefficient on laminar and turbulent Marangoni convection in a typical GTAW pool. The weld pool penetration in case of positive surface tension coefficient is found to be a resultant effect of thermal diffusion and convective mechanisms, whereas the weld pool penetration in case of negative surface tension coefficient is determined predominantly by thermal
diffusion. In a turbulent pool, due to enhanced mixing, the mean convective effect becomes weaker in comparison to that in the corresponding laminar pool, which tends to reduce the pool penetration in case of a positive surface tension coefficient flow. On the other hand, an effective enhancement in diffusion tends to increase the pool depth. These two counteracting mechanisms compete in deciding the resultant pool depth in positive surface tension coefficient flows. In negative surface tension coefficient flows, the weaker mean convection in turbulent pool results a pool of smaller length and width, as compared to that in laminar flow. However, an enhanced diffusion results in a higher weld pool penetration in turbulent pool than that in case of a laminar pool.

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