Effect of Heat Source Parameters in Thermal and Mechanical Analysis of Linear GTA Welding Process

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Double-ellipsoidal volumetric heat with Gaussian distribution of heat intensity is one of the most popular heat source model used in fusion welding process simulations. However, the major difficulty of this kind of heat source model is to define the parameters before start of simulation. It is common practice to define the heat source parameters from experimental measurement of weld dimensions for a particular welding condition that meet the demand of two parameters i.e. weld width and penetration. Till date, the definition of front and rear length of double ellipsoidal is to-some-extent arbitrary in linear welding. A sensitivity analysis shows that this ratio has significant effect on weld dimensions as well as thermal distortion and residual stress of final weld joint. This problem has been addressed in present work where the optimum value of the ratio of front and rear length of double ellipsoidal heat source model is designed within the kernel of an integrated optimization algorithm. The ratio is assumed as function of weld velocity and a suitable functional form is designed over a range of welding current and velocity. The proposed trend of ratio along with optimum values demonstrate fair agreement of experimentally measured weld dimensions for linear gas tungsten arc (GTA) welding process. 3D finite element model of thermal and mechanical analysis is developed and assuming elasto-plastic response of material. Temperature dependent material properties along with latent heat of melting and solidification are incorporated in numerical simulation.

KEY WORDS: volumetric heat source; thermal distortion; residual stress; genetic algorithm; fusion welding.

1. Introduction

In a typical fusion welding process of metals, a heat source is applied locally to the interfaces of the two metals to be joined so that they will be bridged after the liquid metal solidifies. Modelling of this type of welding process is a complex phenomenon and involves a lot of interactive physical phenomena. The research on welding heat source models was started by Rosenthal1,2) and Rykalina3) in early dates back to 1940’s. They provided a thermal conduction model of a moving heat source on solid plates and analytically formulate the temperature fields. Several researchers4,5) worked on Rosenthal-Rykalin formulae to minimize the assumptions and to reduce the errors. Grong et al.4) transformed the Rosenthal-Rykalin formulae into dimensionless version. However, these point and line heat source models are used to predict the temperature history away from the weld pool, and these are not well established to predict the temperature field at the weld pool because of its inherent assumptions. In this regard, the utilization of distributed heat source has been reported by a number of researchers. Pavelic6) introduced the distributed heat source model assuming Gaussian distribution of heat flux. Goldak et al.7) introduced a double ellipsoidal mathematical model for welding heat sources based on a Gaussian distribution of power density in space. Wu8) utilized a three dimensional conical heat source model for keyhole plasma arc welding process.

It is apparent from the literature that the modelling of the heat source is an important issue in numerical computation of temperature distribution. The basic idea of the heat source model is the replacement of the physical process with an appropriate surface heat flux or volumetric heat flux.9,10) The most popular model for the heat input is a double-ellipsoidal, because in many arc welds, the double ellipsoid shape is a good approximation. It has been shown that a Gaussian distribution of power density inside a double ellipsoid moving along the weld path was convenient, accurate, and efficient for most realistic welds with simple shapes.11) However, the major drawback of this heat source model is the selection of heat source parameters. Most of the cases, the selection of the heat source parameters are decided by trial and error methods to validate the experimental results such as time-temperature history or weld dimensions. Considerable accuracy can be obtained when the ellipsoidal parameters are equal to that of the weld pool size. However, from experimental observations, the width and depth of the ellipsoid can be predicted, but the length of the front and rear ellipsoid again it should be estimated by trial and error method to meet the computed results.

Gery et al.12) examined the effect of heat source model parameters (assuming double ellipsoidal heat source) and
found that the welding speed have significant effect in calculating the weld pool shape and size. However, the ratio of front and rear length was considered 1:4 for all simulations irrespective of significant variation of welding velocity. This may not be true for all welding speeds since there may be the possibility of variable heat input per unit length. Azar et al.\textsuperscript{13} presented an analytical approaches to calculate welding heat source parameters (assuming double-ellipsoidal shape) from experimentally determined weld pool shape and size during modelling of gas metal arc welding. The analytically extracted heat source parameters have been successfully used to numerically calculate the temperature distribution of more complex weld pool shape. Kiran et al.\textsuperscript{19} presented a methodology, in which without knowing weld pool dimensions experimentally in advance, the welding heat source model are developed using welding conditions and weld joint geometry.

To increase the accuracy in numerical simulation, researchers have incorporated the temperature dependent material properties,\textsuperscript{15} the effect of magnetic field\textsuperscript{16} and presence of surface active elements.\textsuperscript{15} The temperature dependent thermal conductivity has certain effect in thermal simulation, but specific heat and density have negligible effect.\textsuperscript{15} However the thermal history is better when the average thermal properties are considered. The yield has rich effect whereas thermal expansion coefficient and Young’s modulus have little effect on the distortions and residual stresses calculation.\textsuperscript{15} With the increasing of magnetic field the weld zone area in unreformed stainless steel is less than that of the weld area in deformed stainless steel.\textsuperscript{18} Surface active elements such as oxygen or sulphur present in either shielding gas or in material influence the weld pool shape during GTA welding process.\textsuperscript{17} It is quite obvious that the estimation heat source parameters would be intuitively relay on the experimental weld pool shape and size owing to differential influence of peripheral effect such as magnetic effect, surface active element etc. and also the temperature dependent material properties.

The conduction heat transfer based numerical models also find tremendous application in the calculations of weld distortion and residual stress.\textsuperscript{18–24} where the temperature field over a very large domain is of greater importance in comparison to its local variation in weld pool. Most of these thermal simulations employed Gaussian distributed double-ellipsoidal heat source model and coupled thermo-mechanical simulations followed elasto-plastic response of materials. It was shown that the peak temperature in heat affected zone, fusion zone, and weld boundaries depend on the proper choice of heat source parameters.\textsuperscript{20} Deng\textsuperscript{24} reported a reasonable better prediction of welding distortions and residual stresses of carbon steel including phase transformation effects. It is thus obvious that the mechanical analysis rely on the thermal analysis of welding process. Hence it is important to enhance the reliability in temperature calculation to get reasonably better estimation of residual stress and distortion.

A recent trend of optimizing the process parameters in fusion welding process by integrating a well-tested numerical model with an efficient optimization algorithm has been found in the literature.\textsuperscript{25–28} De and DebRoy\textsuperscript{25,26} evaluated optimized values of uncertain parameters such as effective viscosity, thermal conductivity, and absorptivity from a limited volume of experimental data during conduction mode laser beam welding process by utilizing an gradient based optimization scheme and a numerical heat transfer and fluid flow model. Trivedi et al.\textsuperscript{27} reported an inverse approach to estimate laser absorptivity value using few experimentally measured weld pool dimensions. Bag et al.\textsuperscript{28} calculated a set of uncertain model parameters for laser spot welding from a gradient based optimization algorithm.

In the present work, to address the issue mentioned above, a more practical approach has been adopted to estimate the effect of double-ellipsoidal volumetric heat source parameters in thermal and mechanical analysis of fusion welding process, in particular the effect of front and rear length of the double ellipsoidal heat source model on weld pool dimensions, and thermal distortion and residual stresses. However, the other parameters of double ellipsoidal heat source model are approximated from the experimentally measured weld pool dimensions.\textsuperscript{7,12,29–32} The population-based method such as parent-centric operated generalised generation gap (PCX-G3) genetic algorithm (GA) has been used to find the optimum ratio of front and rear length of double-ellipsoidal heat source model. A 3D finite element thermo-mechanical model has been developed to estimate weld pool dimensions and residual stresses that incorporate the temperature dependent material properties.

2. Theoretical Background

2.1. Heat Transfer Model

The fundamental principle in thermal modelling of welding process is the principle of conservation of energy. The general heat conduction equation for linear welding process can be stated as follows

\[ \rho C_p \left( \nabla T \right) = \nabla \cdot (k \nabla T) + \dot{Q} \] ........................ (1)

where \( \nabla \) is gradient operator, \( \rho \) the density of the material, \( C_p \) the specific heat, \( k \) the thermal conductivity, \( \dot{Q} \) the rate of internal heat generation per unit volume, \( \nabla \) is the velocity vector, \( T \) the temperature. The applied boundary conditions on three dimensional solution geometry of half-work piece are represented schematically in Fig. 1. The welding torch moves along AD. There is no temperature gradient normal to the weld interface i.e. symmetric surface (ABCD). The top surface of the plate subjected to a specified heat flux by the cause of welding arc and the remaining surfaces (except symmetric surface) is subjected to heat losses due to convection and radiation. The natural boundary condition can be represented mathematically as

\[ \text{Fig. 1. Solution geometry with thermal and mechanical boundary conditions.} \]
where \( k_a, q, h, \varepsilon, T_0 \) and \( \sigma \) indicates thermal conductivity normal to the surface, imposed heat flux onto the surface as a result of an external heat source, convection heat transfer coefficient, emissivity, ambient temperature and Stefan-Boltzmann constant, respectively. The first term of Eq. (2) indicates the resultant heat transport by conduction normal to the workpiece surface, the second terms implies the imposed heat flux on the surface, the third and fourth term indicate the heat loss by convection and radiation, respectively from the work-piece surface. Here, total heat loss or gain through boundary is balanced by effective heat conduction normal to the surface at an instant of time. These boundary conditions are mathematically expressed by Eq. (2). In this work to avoid the non-linearity, ‘a lumped heat transfer coefficient’ is used, which combines the radiation and convective heat losses and is expressed as

\[ h_{\text{eff}} = 2.4 \times 10^{-3} \varepsilon \times T^{1.81} \] .......................... (3)

where \( h_{\text{eff}} \) is in W.m\(^{-2}\).K\(^{-1}\) and \( T \) is temperature in K. The emissivity \( \varepsilon \) of stainless steel surface is constant material properties and is considered as 0.9.7,12,20 Therefore, the Eq. (2) can be modified as

\[ k_a \frac{\partial T}{\partial n} - q + h_{\text{eff}} (T - T_0) = 0 \] .......................... (4)

In the present work a Gaussian distributed double ellipsoidal volumetric heat source model\(^7\) is used to predict the thermal behaviour of the fusion welding process. According to the moving coordinate system the power density distribution inside the front quadrant (\( f \)) and rear quadrant (\( r \)) for moving heat source model in Cartesian coordinate system is given by following equations

\[ Q_j(x,y,z) = \frac{6\sqrt{3}f_jQ}{a_b c \pi^{3/2}} \exp \left( \frac{-3x^2}{b^2} - \frac{3(y + v(t - t_0))^2}{a_f^2} - \frac{3z^2}{c^2} \right) \] .......................... (5)

where \( v, \tau \) and \( t \) are the welding speed, a lag factor and time respectively; the fractions \( f_f, f_r \) are the heat deposited parameters in the front and the rear quadrants respectively, and \( a_f, a_r, b, c \) care ellipsoidal heat source parameters. The similar expression is also valid for rear ellipsoidal. The heat intensity \( Q \) is expressed as

\[ Q = \eta VI \] .......................... (6)

where \( \eta, V \) and \( I \) are arc efficiency, arc voltage in (\( V \)) and welding current in (\( A \)) respectively; and the fraction of heat deposition parameter satisfy

\[ f_f + f_r = 2 \] .......................... (7)

The value of arc efficiency in present analysis is considered as 0.6. However, the actual surface heat flux due to arc is replaced by double-ellipsoidal volumetric heat source.\(^7,11\)

**Figure 2** depicts the distribution of volumetric heat density over a double-ellipsoidal. It is obvious from Figs. 2(a) and 2(b) that the peak heat intensity is observed at the centre of heat source and the distribution is Gaussian in nature over the volume of ellipsoidal.

\[ k_a \frac{\partial T}{\partial n} - q + h(T - T_0) + \sigma \varepsilon (T^4 - T_0^4) = 0 \] .......................... (2)

\[ \min : F_n(\chi) = \sum_{i=1}^{n} \left( \frac{W_i^{\text{exp}} - W_i^{\text{cal}}} {W_i^{\text{exp}}} \right)^2 \] .......................... (8)

\[ s.t. \quad \chi^L \leq \chi \leq \chi^U \]

\[ \chi = A_0 + \sum_{i=1}^{m} A_i v^i \] .......................... (9)

where \( A \) is constant, “\( v \)” the weld velocity, and \( n \) is the degree of polynomial. Here, the uncertain parameter \( \chi \) is variable in nature and is computed in each sample point of \( m \) number of total sample welds, \( \chi^L \) and \( \chi^U \) are the lower and upper bounds of design variable \( \chi \). The ratio \( \chi \) is assumed as a polynomial function of weld velocity and is expressed as

\[ \chi = A_0 + \sum_{i=1}^{m} A_i v^i \] .......................... (9)

where \( A \) is constant, “\( v \)” the weld velocity, and \( n \) is the degree of polynomial. Here, the uncertain parameter \( \chi \) is variable in nature and is computed in each sample point of \( m \) number of total sample welds. Therefore, the coefficients of polynomial are uncertain parameters indirectly, if we presume the degree of polynomial. The overall algorithm to calculate the polynomial coefficients are described elsewhere.\(^3\)

\[ \varepsilon_x = \frac{\partial \varepsilon}{\partial x} \quad \varepsilon_y = \frac{\partial \varepsilon}{\partial y} \quad \varepsilon_z = \frac{\partial \varepsilon}{\partial z} \] .......................... (10)

\[ \gamma_{xy} = \frac{\partial \varepsilon}{\partial x} + \frac{\partial \varepsilon}{\partial y} \quad \gamma_{xz} = \frac{\partial \varepsilon}{\partial x} + \frac{\partial \varepsilon}{\partial z} \quad \gamma_{yz} = \frac{\partial \varepsilon}{\partial y} + \frac{\partial \varepsilon}{\partial z} \] .......................... (11)

where \( u, v \) and \( w \) represents displacements in \( x, y, z \) directions respectively; \( \varepsilon_x, \varepsilon_y \) and \( \varepsilon_z \) refer to the normal strains in \( x, y \) and \( z \) directions respectively; and \( \gamma_{xy}, \gamma_{xz} \) and \( \gamma_{yz} \) repre-
sent shear strains in xy, yz and xz planes respectively. Assuming the isotropic material, the thermal strain remains same in three directions and the increment of the total strain is sum of the incremental plastic strain, incremental thermal strain and incremental elastic strain, represented as \( \{d\varepsilon\} = \{d\varepsilon^p\} + \{d\varepsilon^T\} + \{d\varepsilon^e\} \) .......................... (12)

Following Prandtl-Reuss flow rule and von-Mises/yield criteria, the incremental stress can be represented as \( \{d\sigma\} = [D_{\sigma\sigma}][d\varepsilon] - [D']\{\alpha\}(\Delta T) \) ............... (13)

where, \([D_{\sigma\sigma}] = \left[ D' - \frac{\partial f}{\partial \varepsilon} \right] \left[ \frac{\partial f}{\partial \sigma} \right]^T \left[ D' - \frac{1}{3G + E_T} \right] \) .......................... (14)

where \([D']\) depict the elasticity matrix which consists of mechanical properties like Young’s modulus, \(E\) and Poisson’s ratio \(\mu\), \(G\) is shear modulus and \(E_T\) is local slope between stress and plastic strain of specified material. The last term of Eq. (13) represents the thermal strain which may vary depending upon the special temperature distribution. \([D_{\sigma\sigma}]\) is some sort of elastoplastic matrix where the first term in Eq. (14) is due to elastic response of material or recovery of elastic response when the material is in plastic zone. The second term of the Eq. (14) is due to plastic flow of material which is zero when the material is elastic zone only. The function “f” predicts the yield surface and the evolution of the yield surface are governed by the hardening rule. In present case, von-Mises yield surface is considered and bilinear isotropic hardening rule is assumed that may be appropriate for the selected material.\(^{31}\)

The boundary conditions for the mechanical analysis are depicted in Fig. 1. The displacement normal to the weld interface is zero and the weld specimen rigidly clamped on four points (E, F, G, H) and hence all the displacement degrees of freedom are zero at these four points. The solution domain is discretised by 8-noded isoparametric brick element and similar type of elements is considered both for thermal and mechanical analysis.

### 3. Results and Discussions

In the present work, a bead-on-plate GTA welding on 3 mm thick SS304 is considered to validate the numerical results. Table 1 illustrates the welding conditions along with experimentally measured weld width and penetration. However, these experimental results are adapted from independent literature.\(^{38}\) It is obvious from Table 1 that the weld dimensions increase with weld velocity or heat input per unit length \((P/v)\), where \(P\) is weld power \((V*I)\).

Inclusion of temperature dependent material properties in numerical simulation makes the solution non-linear and computational intensive. On the other hand it enhances the accuracy level of calculation. In the present work, the temperature dependent thermal and mechanical material properties of stainless steel are considered\(^{39,40}\) and a cut-off temperature has been set as 3133 K due to availability of data in the literature. The temperature dependent thermal properties such as specific heat, thermal conductivity, and density are used in this analysis. Similarly, temperature dependent mechanical properties such as thermal expansion coefficient, Young’s modulus, yield stress, ratio between the tangential modulus and Young’s modulus, and Poisson’s ratio respectively are considered in present analysis.

A sensitivity study of the ratio of rear and front length of double-ellipsoidal parameter \((a_r/a_f)\) on weld dimensions for data set \(\#4\) in Table 1 is depicted in Fig. 3(a). It has been recognized from the observed trend that the weld dimensions decrease with increase of \(a_r/a_f\) ratio almost in linear way. Actually, high value of \(a_r/a_f\) ratio promotes to reduce the distributed heat intensity that finally reduces the weld pool dimensions. The weld penetration is more sensitive to the change of \(a_r/a_f\) ratio as compared to weld width. Figure 3(b) shows the variation of weld dimensions with respect to weld velocity at two different values of \(a_r/a_f\) ratio. The weld dimensions diminish with increase of velocity that resembles the fact of reducing heat input per unit length. At low velocity (~4 mm/s), the change in dimensions is more as compared to higher velocity (~7.4 mm/s) due to a jump of heat input per unit length. It is noteworthy from Fig. 3(b) that with the increase of \(a_r/a_f\) ratio, the weld dimensions decreases. Therefore, the sensitivity study depicted in Fig. 3 clearly indicates that the \(a_r/a_f\) ratio may not be defined arbitrarily. An organised approach is necessary to find the optimum value of \(a_r/a_f\) ratio corresponding to a particular welding condition. In present work, it is assumed that \(a_r/a_f\) changes with weld velocity and a suitable trend is adopted that fit best considering the present set of experimental data.

The GA based optimization algorithm is integrated with the heat transfer model to investigate the optimum trend of \(a_r/a_f\) ratio. The trend of \(a_r/a_f\) ratio must satisfy the following two constraints: one is \(a_r/a_f = 1:1\) at velocity \(v = 0\) which resembles with stationary heat source and \(a_r/a_f\) ratio tends to infinity at very high velocity. Several functional forms are possible that satisfy these two constraints. However, several trend of \(a_r/a_f\) ratio have been tried and concluded that a polynomial of degree three fits best considering the availability of present set of experimental data. The polynomial function

![Fig. 3. Sensitivity on weld dimensions: (a) \((a_r/a_f)\) ratio for data set \#4 in Table 1, and (b) weld velocity for \((a_r/a_f) = 2:1\).](image-url)
Figure 4(a) describes the initially generated population i.e. the distribution of coefficients ($A_1, A_2, A_3$) to find the best suitable combination of these coefficients to minimize the objective function (in Eq. 8). The feasible range of these coefficients is depicted in Table 2. It is obvious from Fig. 4(a) that the initial population is diversely distributed over the space and having equal chance to reach the global optimum condition. Figure 4(b) shows the objective function space after 90 generations which is assumed as optimum condition corresponding to the objective function value $1.29 \times 10^{-2}$. Further improvement of objective function in successive generations was not possible. The zone marked by enclosed curve (dotted line) in Fig. 4(b) indicated the $F_0$ value less than 0.01. Increasing $A_2$ and decreasing $A_1$ follow similar trend of $F_0$ value which is less than 0.01 and it indicates that there exists several local minimum within the solution space. However, the global optimum values of coefficients achieved in this case are depicted in Table 2.

The optimum trend of $a_r/a_f$ ratio as a function of weld velocity is depicted in Fig. 4(c) where third degree polynomial fits better.

**Table 2. Optimum calculation of coefficients.**

<table>
<thead>
<tr>
<th>Initial range</th>
<th>Optimum value</th>
<th>Objective function</th>
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<tbody>
<tr>
<td>$A_1$</td>
<td>$A_2$</td>
<td>$A_3$</td>
</tr>
<tr>
<td>$1 \times 10^{-1}$</td>
<td>$1 \times 10^{-3}$</td>
<td>$1 \times 10^{-3}$</td>
</tr>
<tr>
<td>$1.8 \times 10^{-1}$</td>
<td>$18 \times 10^{-3}$</td>
<td>$10 \times 10^{-3}$</td>
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Figure 5 shows the three dimensional temperature distribution at three different positions (at 4/5.2 s, 14/5.2 s and 24/5.2 s) with the velocity of 5.2 mm/s corresponding to welding conditions of data set #3 in Table 1 using the optimum value of $a_r/a_f$ ratio. In Fig. 5, the region surrounded by the liquidus temperature 1700 K represents the weld pool, and its intercepts along the Z-axis and X-axis depict the weld penetration the half-width, respectively. Figure 6 shows the comparison between experimentally measured

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\[ \chi = 1.0 + A_1 v + A_2 v^2 + A_3 v^3 \]
weld macrograph (right side) and computed temperature profile (left side) corresponding to data set #3 in Table 1. Moreover, Fig. 7 depicts the quantitative comparison of experimental and computed weld dimensions using the optimum set of $a_r/a_f$ ratio. It is obvious that relatively fair agreement of the shape and size of computed temperature profile endorse the correct estimation of $a_r/a_f$ ratio in the simulation of linear welding process.

To investigate the mechanical behaviour such as residual stress, effective residual displacements, equivalent plastic strains, a sequentially coupled thermo-mechanical analysis is performed. As the front and rear length ratio is not same for all velocities, the heat intensity on front and rear lengths also changes that directly impact on the thermal behaviour of the weld pool as well as mechanical behaviour of surrounding welded zone. Figures 8–10 illustrate the mechanical behaviour when heat source is at the middle of work-piece (point P in Fig. 1) and subsequently solidified to ambient temperature. The residual stress, strain, and displacement are derived along thickness (PS in Fig. 1) and width of plate (PQ in Fig. 1) for the ratio $a_r/a_f$ of 2:1 and 6:1, respectively corresponding to the welding condition of data set #3 in Table 1. It is noteworthy that the influence of cooling rate on mechanical properties of substrate material is not considered in present analysis.

Figure 8(a) depicts the calculated values of the residual equivalent stresses along thickness direction. The maximum value of the residual stress is obtained as 10.7 MPa that is reasonable significant with respect to flow stress of the material. There is also significant variation of the magnitude of residual equivalent stress (maximum ~0.1 MPa i.e. 1%) at two different front and rear length ratios of heat source model. However, at width direction (Fig. 8(b)) the change of residual stress level is more significant that is at the maximum 5 MPa i.e. 15%. Figure 9 depicts the calculated values of the equivalent plastic strain along the thickness and width direction of work-piece, respectively. It is obvious from Fig. 9(a) that at the ratio of 2:1, the magnitude of the equivalent plastic strain is more as compared to the ratio of 6:1. There exists a maximum deviation of equivalent plastic strain of 0.025 i.e. maximum 19% variation is observed when there is change of $a_r/a_f$ ratio from 2:1 to 6:1. However, for both the ratios the magnitude of the plastic equivalent strain varies with the same pattern. Similarly, Fig. 9(b) depicts the equivalent plastic strain in the direction of the width from the centre of the heat source and it is obvious that there is variation of the plastic strain in the order of ~0.007 i.e. 7%. Figure 10 describes the effective residual displacement that refers to the vector sum of the computed displacements in all three normal directions. The maximum value of effective residual displacement at the centre of heat source is 0.35 mm and gradually reduces to 0.025 mm at the border of weld pool. Again it increases to 0.22 mm within heat affected zone. The magnitude of the effective residual displacement is more at $a_r/a_f$ ratio 2:1 as compared to the ratio.
6.1. Figure 10(b) depicts that the computed effective residual displacement along the width of plate that is maximum at the centre and gradually decreases away from the centre. However, the maximum difference of displacement magnitude is ~0.024 mm along depth direction whereas it is ~0.020 mm along width direction. Overall, it is observed that the variation of properties is more significant along thickness direction as compared to the width of plate. This may be attributed by the fact the heat transfer is more turbulent along thickness direction whereas due to large in dimension along width, the change of temperature distribution is less vibrant. This temperature distribution directly affects the mechanical analysis.

Since direct estimation of front and rear length of a double ellipsoidal heat source model is nearly intractable for fusion welding process, the demand of reliable quantitative models for heat transfer is ever increasing. The present work is a contribution in this direction. Further validation of the calculated results of weld pool dimensions, temperature distribution, and associated thermal distortion for different welding conditions are necessary to reliably establish the process model reported in this work.

4. Conclusions

A coupled thermo-mechanical model was developed and investigated the thermal and mechanical behaviour of the welded plate at different heat source model parameters. This study also gives some means to reduce the mechanical effects in designing the welding process. From this present work it may be concluded as follows: (i) the influence of the rear and front length ratio on weld pool dimensions is significant, and when \(a_f/a_r\) ratio increases the weld pool dimensions decrease.(ii) There exists a suitable functional form of \(a_f/a_r\) ratio that varies with weld velocity and third degree polynomial represent better this functional form. (iii) The simulated thermal results well agree with experimentally measured weld dimensions at optimum values of \(a_f/a_r\) ratio. (iv) The variation of calculated values of final residual displacement, equivalent plastic strain and residual stress has indicated that precise information of front and rear length ration is necessary in thermal and mechanical analysis. However, the calculated values of thermal distortion and residual stress require further verification with experimental results. Moreover, the present stress analysis needs to be enhanced to account the metallurgical effect due to phase change and cooling rate.

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