Analysis of Thermal Cycle during Multipass Arc Welding

A new method is proposed to account for convection and radiation heat losses from the surface during simulation of multipass welding thermal cycle

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ABSTRACT

Convection and radiation heat loss from the plate surface during multipass gas tungsten arc welding (GTAW) plays a very important role in deciding peak temperature. The heat losses from the surface can be efficiently incorporated in finite element formulation, but it is very difficult to derive an analytical expression for the same. A new method is proposed to account for convection and radiation heat losses from the surface during simulation of a multipass welding thermal cycle. The proposed method finds the temperature correction term for temperature distribution estimated using conduction solution. Simulation results of an approximate analytical solution are compared with experimental and finite element simulation results. Heat loss at thermocouple-plate junction due to contact conductance resistance induces error in temperature measurement. The temperature drop due to this effect at the thermocouple junction is compensated by considering this loss.

Introduction

Neglecting convection and radiation heat losses is very difficult to achieve. The solution considering convection and radiation heat loss from the surface during simulation of a multipass welding thermal cycle. The proposed method finds the temperature correction term for temperature distribution estimated using conduction solution. Simulation results of an approximate analytical solution are compared with experimental and finite element simulation results. Heat loss at thermocouple-plate junction due to contact conductance resistance induces error in temperature measurement. The temperature drop due to this effect at the thermocouple junction is compensated by considering this loss.

A durability assessment of a weld joint needs the knowledge of residual stresses and distortion (Ref. 1). Thermal cycle history during welding is a necessary input for simulation of residual stresses. Many investigators have studied heat flow during arc welding analytically, numerically, and experimentally (Refs. 2–6). Analytical methods developed are capable of computing temperature distribution with reasonable accuracy. An approximate analytical solution for plate with finite thickness using Green’s function and effective heat source to compensate for Neumann boundary condition (Ref. 2) has provided a very efficient tool to quickly simulate the transient thermal cycle during welding. This closed form solution has a distinct advantage over a finite element procedure and doesn’t need a tedious procedure of modeling and discretization to be followed. This solution can be extended to multipass welding using the principle of superposition; however, a closed form solution considering convection and radiation heat losses is very difficult to achieve. Neglecting convection and radiation heat loss for the weld pool gives relatively high peak temperature values.

Numerical techniques such as the finite element method are used increasingly by researchers particularly for complex weld geometries but this requires “tuning” or calibration of the heat source to get a solution with acceptable accuracy. In the present research work, a method to extend an approximate analytical solution for multipass welding is proposed and a temperature correction term is derived to account for convection and radiation heat loss from the weld. The authors have verified the proposed analytical solution experimentally and also with finite element simulation results obtained from ABAQUS to an acceptable accuracy of 90% for the problem being investigated.

Temperature Rise for Volume Heat Source in Finite Body

The temperature rise during period $t'$ for volume heat source in a finite body is given by

$$T(x, y, z, t') - T_0 = \int \int \int t' \frac{Q(x', y', z', t')}{\rho c} dx' dy' dz'$$

$$G_{fin}(x, y, z; x', y', z', t') dx' dy' dz'$$

$G_{fin}$ in Equation 1 is Green’s function for a point heat source in the finite body that satisfies the Neumann boundary condition of zero heat density ($\partial T/\partial n = 0$, where $n$ is the normal direction) across its boundary surfaces (Ref. 7). Finding an analytical solution for $G_{fin}$ would be almost an impossible task. An alternate approximate approach to compensate for the Neumann boundary condition when dealing with a finite body has been proposed by researchers (Ref. 2). In this approach, the same Green’s function for the point source in an infinite body is used, but the heat source in an infinite body is replaced by the effective heat source $Q_{eff}(x', y', x, t')$ in the finite body. The effective heat source produces the same amount of heat into the finite body as the original heat source would in an infinite body. Using this approach, an approximate temperature field in a finite body subjected to volume heat is estimated by Equation 2.

$$T(x, y, z, t') - T_0 = \int \int \int t' \frac{Q(x', y', z', t')}{\rho c} dx' dy' dz'$$

$$G_{inf}(x, y, z; x', y', z', t') dx' dy' dz'$$

The effective heat source $Q_{eff}(x', y', x, t')$ in the finite body approximately compensates for the Neumann boundary condition when dealing with a finite body, which enables the use of the same Green’s function for the point source in an infinite body.

Modeling Multipass Welding

The modeling of multipass welding is more complex and difficult than single-pass welding due to repeated phase
changes and annealing. Approximate analytical solution for multipass welding is not yet reported. It is proposed in the present research work to use a method of superposition in time domain and changing absolute time with differential time in Equation 2. Thus, the time corresponds to the sum of welding and waiting time prior to beginning of the succeeding pass. Variation in the amount of heat input in each pass is taken into account. Dwell time is accounted by converting real heat source at the end of the weld run, to fictitious zero value heat source traveling beyond the plate length.

To include the effect of variable thermal properties in an analytical expression is a very complex and difficult task. Therefore for better accuracy, temperature-dependent thermal properties of the workpiece were updated while solving the integral part of Equation 2 using the numerical method.

**Engineering Approach to Account for Heat Loss to Surrounding Surface**

Heat loss occurs from the material surface during welding by both convection and radiation. It is proposed to find a combined heat transfer coefficient to take into account the convection and radiation heat loss. The surface heat flux due to this is then found using the combined heat transfer coefficient. As an inverse problem, it is assumed that an instantaneous plane heat source equal to heat loss is acting, and temperature distribution due to this is calculated and then subtracted from the earlier calculated temperature field. Temperature correction done using this approach effectively compensates for convection and radiation heat loss from the boundary. An empirical relationship proposed by Vinokurov, \( h = 24.1 \times 10^{-4} \varepsilon T^{1.61} \) (Ref. 6, 8, 9) to account for the combined effects of radiation and convection. Researchers (Ref. 10) have used simplified equations and terms for convection and radiation loss for infinitely thin plate. However, estimation of quantum of heat loss due to convection and radiation from the weld plate surface has not been reported. In the present research work, the following procedure is adopted to find a combined temperature-dependent convective and radiation heat transfer coefficient. Convective heat transfer coefficient is estimated using Equation 3 (Ref. 11).

\[
h = \frac{Nu \ k_{\text{air}}}{L_c}
\]  
(3)

Where, \( Nu \) is Nusselt number, \( k_{\text{air}} \) is thermal conductivity of air, and \( L_c \) is characteristic length. Heat loss due to radiation is accounted for by calculating the equivalent heat transfer coefficient \( h_{\text{rad}} \) using Equation 4.

\[
h_{\text{rad}} = \varepsilon \alpha \left( T_{s}^{2} + T_{a}^{2} \right) \left( T_{s} + T_{a} \right)
\]  
(4)

Where \( \varepsilon \) is emissivity of body surface, \( \alpha \) is Stefan Boltzmann constant, \( T_s \) is plate surface temperature, and \( T_a \) is atmospheric temperature. This definition of the radiation heat transfer coefficient is analogous to convection in terms of a temperature difference (Ref. 11). Effective heat transfer coefficient considering combined convection and radiation is calculated using Equation 5.

\[
h_{\text{eff}} = h + h_{\text{rad}}
\]  
(5)

Figure 1 shows the comparison be-
between Vinokurov’s solution and the proposed approach. The proposed method gives higher values of $h_{eff}$ than calculated from Vinokurov’s solution, the difference being 10–20 up to 400°C.

For heat liberated to the surrounding area from the surface, $Q_a$ is calculated using Equation 6.

$$Q_a = h \left( T_a - T_b \right)$$

where $A =$ area from which heat is being liberated to the surrounding area (assumed to be 5% of the total area). The required temperature correction, $T_c$, to compensate for convection and radiation heat losses is calculated using Equation 7 (Ref. 7).

$$T_c = \frac{Q_a}{2 \pi \alpha t} e^{-\frac{(x-x')^2}{4 \alpha t}}$$

Where $\alpha$ is thermal diffusivity, $x' = x - vt$ is dimension in moving coordinate system, and $t$ is time. It should be noted that one-dimensional heat flow is assumed in the above expression, which is quite realistic for instantaneous plane heat source. Finally, the corrected temperature field is calculated as

$$T(x,y,z,t) = T(x,y,z,t) - T_c (z,t)$$

The computer program $SANARC$ was developed to calculate the integral expression in Equation 2 using Gauss quadrature numerical technique for the double semi-ellipsoidal distributed heat source during multipass welding. For the same discretized time interval, temperature correction term for convective and radiation heat losses was calculated using Equation 7, and the temperature field was accordingly updated. The result of program $SANARC$ developed to account for convection and radiation losses using an engineering approach was later compared with finite element simulation results obtained from the program $ABAQUS$ and with an experimentally measured thermal cycle.

**Case Study — Welding of Duplex Stainless Steel Plates**

Duplex stainless steel has good weldability, and it can be easily welded by both manual and automatic gas tungsten arc welding (GTAW), gas metal arc welding (GMAW), plasma arc welding (PAW), shielded metal arc welding (SMAW) with covered electrodes, flux cored arc welding (FCAW), and submerged arc welding (SAW). Although weldability of duplex stainless steel is good, an important precaution is to limit as much as possible the holding time at an intermediate temperature between 300° and 980°C. Low thermal expansion in duplex grades reduces distortion and residual stresses after welding. The solidification of the duplex alloy is not prone to hot cracking due to low impurity levels; however, it may occur under high-restraint conditions (Ref. 12). The weldment specimen was prepared as per the drawing shown in Fig. 2.

**Modeling Considerations**

1) A combined convection and radiation boundary condition as calculated from Equation 5 is used on the top surface and $\varepsilon = 0.9$ was assumed.
2) No forced convection was assumed, and the effect of gas diffusion in the weld pool was not considered.
3) Neumann boundary condition was assumed during calculation of initial (uncorrected) temperature field.
4) Welding speed was assumed constant.

**Welding Parameters**

The geometric parameters for a double ellipsoidal heat source used in simulation are listed in Table 1. The parameters mentioned in Table 1 are in meters and based on the specimen geometry and a weaving

<p>| Table 1 — Double Ellipsoidal Geometry Parameters |</p>
<table>
<thead>
<tr>
<th>Pass</th>
<th>$a_h$</th>
<th>$b_h$</th>
<th>$c_{hf}$</th>
<th>$c_{hb}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>0.001</td>
<td>0.002</td>
<td>0.012</td>
<td>0.02</td>
</tr>
<tr>
<td>2</td>
<td>0.002</td>
<td>0.002</td>
<td>0.012</td>
<td>0.02</td>
</tr>
<tr>
<td>3</td>
<td>0.003</td>
<td>0.002</td>
<td>0.01</td>
<td>0.02</td>
</tr>
<tr>
<td>4</td>
<td>0.0045</td>
<td>0.003</td>
<td>0.01</td>
<td>0.02</td>
</tr>
<tr>
<td>5</td>
<td>0.006</td>
<td>0.003</td>
<td>0.01</td>
<td>0.02</td>
</tr>
</tbody>
</table>

<p>| Table 2 — Welding Parameters |</p>
<table>
<thead>
<tr>
<th>Pass</th>
<th>Interpass Heat Input per Dwell Unit Length (HI) (kJ/mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>6.322</td>
</tr>
<tr>
<td>2</td>
<td>6.438</td>
</tr>
<tr>
<td>3</td>
<td>7.835</td>
</tr>
<tr>
<td>4</td>
<td>8.932</td>
</tr>
<tr>
<td>5</td>
<td>7.145</td>
</tr>
<tr>
<td>Total</td>
<td>36.672</td>
</tr>
<tr>
<td>Avg.</td>
<td>7.335</td>
</tr>
</tbody>
</table>

(a) efficiency is not considered.
The pattern adopted while welding.

Interpass dwell time and heat input used during experimental investigations (Table 2) were used as an input for simulation. Higher interpass dwell time after the second pass was used to minimize the effect of repeated heat exposures during multipass welding.

Material Properties

Material data, which change during a weld thermal cycle and during phase transformations, are usually missing (Ref. 13) and are one of the major factors for inaccuracies in simulation (Ref. 14). Researchers (Refs. 15–17) have studied the effect of variable material properties on welding simulation. The temperature-dependent physical properties of duplex stainless steel, as stated in Table 3, were used for the simulation.

Sensor Location

Material data, which change during a weld thermal cycle and during phase transformations, are usually missing (Ref. 13) and are one of the major factors for inaccuracies in simulation (Ref. 14). Researchers (Refs. 15–17) have studied the effect of variable material properties on welding simulation. The temperature-dependent physical properties of duplex stainless steel, as stated in Table 3, were used for the simulation.

The thermal cycle was simulated for the thermocouple locations mentioned in Table 4. In this table, x is the distance from the weld start position, and y is the distance from the weld center. These results are from the simulation program SANARC as shown in Fig. 3.

The use and application of the proposed analytical solution SANARC are mentioned below.

1) Using simulated transient temperature data and cooling rates, the austenite-ferrite phase balance can be estimated, which is very useful information for the dual-phase alloy.

2) Heat-affected zone width can be estimated from temperature data for various combinations of net heat input and weld torch travel speed, which will be useful in deciding these parameters.

3) The temperature distribution obtained will be useful to estimate the residual stresses and any consequent degradation in the mechanical properties of the welded joint.

Simulation of Welding Thermal Cycle Using Finite Element Method

The finite element method is the most widely used simulation technique due to its flexibility to adopt complex geometry and boundary conditions. Finite element code ABAQUS is used to simulate transient thermal cycle during multipass welding. ABAQUS has the capability to model solid body heat conduction with general, temperature-dependent conductivity; internal energy (including latent heat effects); and quite general convection and radiation boundary conditions. Energy is related to temperature in terms of a specific heat, neglecting coupling between mechanical and thermal behavior. Latent heat effects at phase changes are given separately in terms of solidus and liquidus temperatures. When latent heat is given, it is assumed to be in addition to the specific heat effect, and heat conduction is assumed to be governed by the Fourier law.

### Table 3 — Physical Properties of Duplex Stainless Steel

<table>
<thead>
<tr>
<th>Temperature °C</th>
<th>20</th>
<th>50</th>
<th>100</th>
<th>200</th>
<th>300</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density (kg/m³)</td>
<td>7805</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Thermal conductivity (W/m.K)</td>
<td>17</td>
<td>17</td>
<td>18</td>
<td>19</td>
<td>20</td>
</tr>
<tr>
<td>Specific heat J/kg °C</td>
<td>450</td>
<td>450</td>
<td>500</td>
<td>530</td>
<td></td>
</tr>
</tbody>
</table>
Spatial Discretization and Time Integration

Standard Galerkin approach is used for variational formulation of the energy balance, together with the Fourier law. The backward difference method is used for time integration time increment is calculated using an automatic (self-adaptive) time-stepping algorithm. This is based on a user-supplied tolerance on the maximum temperature change allowed in a time increment, and the increment is adjusted according to this parameter.

Finite Element Model Data

Diffusive first-order heat transfer element DC3D8 (8-node brick) is used for discretization of weld plate geometry, which provides accurate solutions with large latent heats. Interpolation is defined in terms of the isoparametric element. The finite element model of plates to be welded as shown in Fig. 4 consists of 9360, eight-noded brick (hexahedral) elements with filler material modeled in five distinct layers to represent five weld passes. An initial temperature of 35°C is assigned to all nodes of the model.

Procedure

Heat transfer procedure as defined in ABAQUS is used to simulate the transient thermal cycle. Weld elements are deactivated initially and successively added to simulate the addition of material during each pass. Body flux is defined in the weld filler element, while surface flux is given on the slant face of the V-groove. Convective heat transfer from the plate surface is simulated by considering combined convective and radiation heat transfer coefficient. A significant portion of heat is convected and radiated to the surrounding area from the molten weld pool because of strong convection currents and forced convection due to the flow of argon gas over the area. This heat is accounted for by tuning the heat input to achieve the fusion boundary temperature equal to solidus temperature. The welding parameters as defined in Table 2 were used during the analysis.

Results

Simulation results of temperature-time thermal cycles for six distinct nodes from ABAQUS uncoupled transient heat transfer analysis are shown in Fig. 5. Distance of the node from the weld centerline is given in the bracket next to the node number. These results are obtained by requesting history data output during the ABAQUS simulation run. Since the solution domain is spatially discretized, history data output for the temperature-time plot cannot be obtained exactly at every sensor location used in experimentation. Therefore, output of history data requested at thermocouple sensor location y = 9 mm is used for comparison purpose.

The Experimental Investigations of Transient Thermal Cycle

To verify the simulated transient temperature profile as obtained in earlier section temperature histories, six points on the plate were recorded during welding.

Temperature Sensing Device

Temperature measurement in the present work was done by using K-type (chromel-alumel) thermocouples. They are cheap and interchangeable, have standard connectors, and can measure a wide range of temperatures. The transient temperature distribution during welding was recorded using a precalibrated 12-channel data acquisition system manufactured by Yokogawa Corp. The system had a programmable interface with in-built memory along with portable storage media functionality USB 2.0 data cards.

Test Rig Design and Fabrication

The test rig was designed to meet the two requirements of easy and continuous welding and simultaneous measurement of the temperatures using thermocouples. Chromel-alumel thermocouples were attached to the plate from the bottom to measure the temperature distribution at different distances from the weld centerline. This was done to make the welding operation easier for the operator. The temperatures were measured at the middle plane of the plate by drilling holes of diameters equal to that of the sensing rods of the thermocouples, with sufficient clearance provided so that the sensing head made positive contact with the plate at the correct measuring node. The data points were chosen to be staggered along the weld run so that at every instant the sensor would ideally be able to “see” the heat source without any obstruction. The aim of the experimentation to validate the simulation program, hence the locations of thermocouple, was selected so as not to follow a particular pattern. Care was taken to avoid are strike and termination zone. The thermocouples were mounted on foam blocks to ensure the required contact stiffness as shown in Fig. 6. This is more practical than welding the thermocouples to the plate for every experiment. Glass wool insulation was provided around the thermocouples to protect them from radiant heat.

The welding was carried out using 2209 filler metal of diameter 1.6 mm, as per AWS 1554.6 guidelines. The use of 2209 filler metal ensured the deposited metal was the correctly balanced duplex structure because of increased nickel percentage, which is a strong austenite former. A weaving pattern was adopted for depositing the filler metal. Cleaning and degreasing of the weld area was done prior to welding and fine grinding was done to remove weld spatter and undercut after welding. The welding parameters used are listed in Table 2.

Thermal Contact Resistance

Thermocouple and plate (as seen in Fig. 6) can be considered a composite system, as shown in Fig. 7, in which the temperature drops appreciably across the interface between them. This temperature drop is attributed to the thermal contact resistance $R_{t,c}$ (Ref. 18), and for a unit area of the interface, the resistance is defined as

$$R_{t,c} = \frac{T_s - T_k}{q_t^*}$$

The existence of a finite contact resistance is primarily due to a surface roughness effect. Heat transfer is therefore due to conduction across the actual contact area and also due to conduction and/or radiation across the gaps. Thus the contact resistance consists of two parallel resistances, one due to the contact spots and the other due to the gaps. The contact area is typically small, and especially for rough surfaces, the major contribution to the resistance is made by the gaps.

Surface contact resistance $R_{t,c}^{\text{surf}}$ is estimated to vary linearly from 30 to 300 m$^2$·°C/W (Ref. 18) in the temperature range of 35°C to 700°C over contact area defined by a circle of 0.25-mm radius. Heat transfer at the junction, estimated from welding parameters and temperature sensed by thermocouples, was used to calculate temperature at the plate surface considering loss due to thermal contact resistance using Equation 9. The corrected measured thermal cycle still included unavoidable experimental errors, such as locating the hole depth to which the thermocouple is in contact with plate.

Transient temperature during the welding was measured at six locations as mentioned in Table 4, and the associated thermal cycle is shown in Fig. 8. The thermocouple number and its distance from the weld centerline (in bracket) is mentioned in the plot. Comparison of the corrected temperature at the plate surface with the simulated cycle is shown in Fig. 9 for sensor location 1 ($y = 9$ mm) of the plate.
Conclusions

- An approximate analytical solution as proposed by Nguyen et al. was extended to simulate multipass welding using principles of superposition and fictitious source method. A procedure based on an engineering approach was developed to find temperature correction terms to account for convective and radiation heat losses. The heat losses were accounted for while solving the integral expression in time domain. The proposed solution was implemented by developing the computer program SANARC, and marginal reduction in peak temperature was achieved.

- The finite element code ABAQUS was used to simulate a transient thermal cycle with complex boundary conditions of moving heat source and temperature-dependent combined convective and radiative heat transfer. Addition of filler metal and latent heat effects were also considered in the program. Results obtained from the finite element solution show higher peak temperature than the approximate analytical solution.

- Transient temperatures were recorded at the locations mentioned in Table 4 during welding using modern and rugged instrumentation. The lower peak temperature for the spring-loaded thermocouple was due to an insufficient contact between the thermocouple and the plate, which resulted in some loss of heat due to contact resistance.

- Preflow and postflow of argon gas before start of the weld pass and after completion of the weld pass significantly reduced the interpass temperature and interpass dwell time in the first two passes; however, both dwell time and interpass temperature increased marginally in the last two or three passes.

- The proposed solution SANARC is computationally more efficient than the finite element method and does not require the time-consuming process of discretization of domain. It is also useful in more accurate simulation of residual stresses and microstructure at less computational cost.

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