A Two-Dimensional Thermomechanical Analysis of Burn-Through at In-Service Welding of Pressurized Canals

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Abstract: In this study a numerical model has been developed to predict the onset of the failure of a rectangular canal-wall during an in-service welding process. In-service welding is one of the many applications of welding methods which has a wide use in petrochemical and gas industries. The safety of the in-service welding is a major concern and the investigation of the Burn-Through and Hot-Cracking risks is becoming an important issue. In this research a Finite Element based numerical method has been used to model the in-service welding of an AISI 304 stainless steel plate on a rectangular canal-wall. A coupled 2D thermo-elasto-plastic FE model has been developed in which the temperature dependency of the physical properties of the material has been taken into account. Also, the effect of different factors such as: pressure of the fluid passing through the canal, different types of supports and geometry of the weld beads to reduce the risk of the burn-through have been studied. In addition, temperature-time diagrams have been produced which can be used to study the possibility of hot-cracking. The results show that the burn-through occurs under the welding pool and it is more likely to happen in the first welding-pass. These results show that the thermal condition of the fluid passing the canal, the external loadings such as internal pressure and supports have significant effect on the thermal stresses which may cause burn-through.

Key words: In-service welding, burn-through, FEM, Thermo-elasto-plastic analysis, canal wall

INTRODUCTION

Welding is a reliable and efficient metal joining process. During the three past decades many researches have been carried out to predict the temperature fields and stress distribution in sheet and pipe weldments. Francis et al. (2007) have reviewed the metallurgical issues that arise in ferritic steel welds and related these to the difficulties in calculating residual stresses. Analytical methods hardly can be used for this purpose because of the complexity of the boundary conditions; high order non-linearity of the governing constitutive equations and change of material properties with temperature during welding process. Also, due to the required expensive tests, many of the recent researches are based on Finite Element methods and simulation. Therefore, numerical methods have a useful role in assessment of welding condition or the safe in-service welding of high-pressure fluid canals or pipelines. Price et al. (2008) have used the neutron diffraction technique to measure the residual stresses due to the welding and have compared the data with those obtained using finite element procedures. The results indicated good qualitative agreement. Two-dimensional (2-D) axi-symmetric models have been employed to numerically simulate a series of multi-pass circumferential butt-welds of stainless steel pipe in a non-linear thermo-mechanical finite element analysis (Brickstad and Jøsefson, 1998). Also a review of numerical weld simulation has been provided by Yaghi et al. (2006) where, the finite element methodology has been described in detail and applied to welded stainless steel pipes. A 2-D finite element model has been developed by Armentani et al. (2007) to analyse the temperature distribution in butt welded joints. In this study the influence of thermal properties and preheating was investigated on the residual stresses in welding. A more general 3D finite element method has been developed by Goldak et al. (2000) to calculate the thermal fields for predicting burn-through on pressurized natural gas pipelines. Their results show that the assumed weld bead size and shape of the fillet weld have a significant influence on the calculated penetration and temperature profile around the weld pool. Sabapathy et al. (2000) have investigated the burn-through of gas pipelines during in-service welding with the use of numerical methods. A new mathematical description of the heat-source for Manual Metal Arc (MMA) welding has been introduced in their study. Also limited data has been given and related to the
temperature profile of the in-service welding. In another research (Wahaba et al., 2005), a new approach to the problem of burn-through has been developed based on the thermo-elasto-plastic models which directly simulate pipe bursting during welding. In this study a 3D model has been developed to investigate the pipe-wall failure during the in-service welding and under the pressure of the hot fluid passing through the pipe. Xiaolong et al. (2006) have established a method of predicting design pressure and burn-through of in-service welding under variable parameters using FEM. In this study, it has been shown that the design pressure decreases with the increasing of heat input. Although limiting the heat input is also an important consideration to prevent burn-through, but the potential for rapid melting of the weldment increases its susceptibility to hydrogen assisted cold cracking or HACC (Nolan et al., 2005).

During in-service welding of pipes and canals two primary concerns are burn-through and cracking. According to the description of American Petroleum Institute (API) burn-through will occur if the un-melted area beneath the weld pool can no longer contain the pressure within the canal or pipe. Figure 1 shows the type of wall failure (burn-through) due to the in-service welding. Weld cracking results when fast weld cooling rates produce a hard and crack-susceptible weld microstructure. Fast cooling rates can be achieved by flowing contents inside the canal or pipe which remove heat quickly (API, 2003).

In this study, a Finite Element based model has been used to study the temperature and stress fields beneath the weld pool for an in-service welding of a SUS304 stainless steel plate on the rectangular canal-rid. In this model, the mechanical properties have been considered temperature dependent. The welding has been modeled using Element Birth and Dead technique with three welding passes. Depending on the thermo-mechanical conditions of the fluid passing the canal, this study has been carried out in three cases: basic, first and second case. Using this model, an in-service welding can be modeled and the risk of the burn-through and hot-cracking can be assessed which is a major concern in petrochemical and gas industries.

**ANALYSIS MODEL**

In simulation of the in-service welding procedure, the interaction of the thermo-mechanical fields has to be taken into account. Two different methods: un-coupled and coupled methods can be used to solve the governing constitutive equations. In the former method, firstly, the thermal analysis of the process is solved independently, and then, using these results, the mechanical stress analysis can be carried out which considers the contributions of the transient temperature fields through thermal expansion. In the coupled method which has been used in this study, the thermo-mechanical analysis are solved together (Deng and Murakawa, 2006). In this way, the mutual interaction between temperature fields and strain distribution of the material would be considered which may have a significant effect because the dimensional changes are considerable in welding and therefore, mechanical work due to these displacements cannot be ignored. The thermo-mechanical constitutive equations which have been used to model the welding process are presented below.

**Thermal model:** The fundamental equation of heat conduction is:

\[
\rho c \frac{\partial T}{\partial t} = \frac{\partial}{\partial x} \left( K \frac{\partial T}{\partial x} \right) + \frac{\partial}{\partial y} \left( K \frac{\partial T}{\partial y} \right) + \frac{\partial}{\partial z} \left( K \frac{\partial T}{\partial z} \right) + Q = \left( L T^a \right) \left( D \left( D^a \right) T \right) + Q
\]

and the boundary condition of the heat convection over the control volume surface is:

\[
q^T \left[ r \right] = -h_s \left( T_s - T_{ad} \right)
\]

The expanded form of Eq. 2 is:

\[
K_x \frac{\partial T}{\partial x} + K_y \frac{\partial T}{\partial y} + K_z \frac{\partial T}{\partial z} + q_i + \left( h_i + h_{ad} \right) \left( T_s - T_{ad} \right) = 0
\]

In these equations \( \rho \) is density, \( c \) is specific heat, \( T \) is temperature, \( t \) is time, \( q \) is vector of heat flux, \( q_i \) is the external heat flux, \( Q \) is the rate of internal heat generation, \( n \) is the unit outward normal vector, \( h_i \) is the total film coefficient, \( h_{ad} \) is the convection film coefficient, \( h_i \) is radiation film coefficient, \( T_s \) is the bulk temperature of the adjacent fluid and \( T_{ad} \) is the temperature at the surface of the model.
Pre-multiplying the governing equation by virtual charge in temperature $\delta T$ and integrating over the volume of the element, considering the boundary condition, Eq. 1 yields

$$
\int \left( \delta T \frac{\partial T}{\partial t} \right) dV + \int \left( \delta T \left[ \frac{\partial}{\partial t} \right] \left[ \frac{\partial}{\partial t} \right] T \right) dV = 
\int \left( \delta T \left[ q_i + (h_i + h_e) \right] (T_0 - T_i) \right) dA$

where, the variable $T$ varies in both space and time domain. Equation 4 can also be re-expressed as:

$$
\int \left[ \frac{\partial T}{\partial t} \right] dV + \int \left[ \frac{\partial}{\partial t} \right] \left( \frac{\partial T}{\partial t} \right) dV = 
\int \left[ \frac{\partial T}{\partial t} \right] \left[ q_i + (h_i + h_e) (T_0 - T_i) \right] dA
$$

where, $[N]$ are element shape functions and $\{T_0\}$ is the nodal temperature vector. Equation 5 can be rewritten as:

$$
\frac{\partial \{T\}}{\partial t} + \{K\} \{T\} = \{F_i\}
$$

$$
\frac{\partial \{T\}}{\partial t} = \{T\} + \frac{\Delta T}{\Delta t}
$$

Where:

$[C] = \int [\rho c][N]^T [N] dV$

$[K] = \int [B]^T [D] [B] dV + \int \left( h_i + h_e \right) [N]^T [N] dA$

$\{F_i\} = \int [N] q dV + \int \left( h_i + h_e \right) T_0 dA$

The temperature field in thermal model analysis may be obtained from Eq. 6 (Tenga and Changb, 1998).

**Mechanical model:** According to the principle of virtual work and the divergence theorem, the equilibrium equations and the constitutive equations can be rewritten in the matrix form as:

$$
\int \xi \left( \sigma \right)^T dV = \int \xi \left( \delta u \right)^T [P] dA + \int \xi \left( \delta u \right)^T \{f\} dV
$$

Let

$$
\{\xi\} = [B] \{U\}, \ \{\sigma\} = [B] \{\varepsilon\}, \ \{u\} = [N] \{U\}, \ [B] = [L][N][S]
$$

where, $\{P\}$ is the surface force vector, $\{f\}$ is the body force vector, $\{\varepsilon\}$ is the displacement vector, $\{\sigma\}$ is the strain vector, $\{\varepsilon\}$ is the stress vector, $\{U\}$ is the nodal displacement vector, $[N]$ is the matrix of the shape function and $[L]$ is the differential operator, $[B]$ is the operator matrix. Substituting Eq. 8 into Eq. 7 yields:

$$
\int [B]^T \{\varepsilon\} dV = \{R\} = [K] \{U\}
$$

Where:

$$
\{R\} = \int [N]^T \{P\} dA + \int [N]^T \{f\} dV
$$

is the nodal equilibrium external force matrix and

$$
$$

is the stiffness matrix.

The above expressions are assumed to represent a linear elastic model. However, to model the welding process, it is necessary to solve the equations based on a non-linear isotropic hardening elasto-plastic theory. For this purpose, a bi-linear elasto-plastic formulation of the material behaviour has been used. Also, an incremental calculation has been employed to accommodate the nonlinearity of the nodal displacement functions in the elasto-plastic analysis. For the incremental analysis, the load $\{R\}$ at step $m + 1$ may be expressed as:

$$
\{R\} = \{R\} + \{\Delta R\}
$$

If the solutions of $^n \{U\}, ^n \{\sigma\}$ at the $n$th step are assumed to be known, the solutions of the $(m + 1)^{th}$ step can then be obtained as:

$$
^m \{U\} = ^{n+1} \{U\} + \{\Delta U\}, \ \text{and} \ \ ^m \{\sigma\} = ^{n+1} \{\sigma\} + \{\Delta \sigma\}
$$

Accordingly, Eq. 9 and 10 become:

$$
\int \xi \left( \sigma \right)^T dV = ^n \{R\} + \{\Delta R\} - \int \xi \left( \sigma \right)^T \{\varepsilon\} dV
$$

Substituting Eq. 9 into Eq. 12 yields:

$$
\int [B]^T \{\Delta \sigma\} dV = \{\Delta R\}
$$

Using the thermo-elasto-plastic material model, based on the Von-Mises yield criterion and the isotropic strain hardening rule, stress–strain relations can be written as:

$$
\{\Delta \sigma\} = (D^e) \{\Delta U\} - (C^m) \{\Delta T\}
$$

where, $(D^e) = (D^0) + (D^p)$ and $\{\Delta \sigma\}$ is the nodal stress increment matrix, $\{\Delta \sigma\}$ is the nodal strain increment matrix, $\{\Delta T\}$ is the temperature increment matrix, $\{D^0\}$ is the elastic stiffness matrix, $\{D^p\}$ is the plastic stiffness matrix, $\{C^m\}$ is the thermal stiffness matrix, $\{\Delta T\}$ is the nodal temperature increment matrix and $\{M\}$ is the

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Substituting Eq. 14 into Eq. 13 yields:

\[ n n_i \{ K_i \} \{ \Delta U_i \} = n i \{ K_i \} \{ T_i \} = \{ \Delta R_i \} \]  
\[ (15) \]

Where:

\[ n i \{ K_i \} = \int \int [B]^T \{ D^n \} [B] dV \]

and

\[ n n_i \{ K_i \} = \int \int [B]^T \{ C^n \} [M] dV \]

The displacement increment \( \{ \Delta U \} \) and stress increment \( \{ \Delta \sigma \} \) can be obtained from Eq. 14 and 15. With these results, the displacement \( \{ U \} \) and stress \( \{ \sigma \} \) are then obtained from Eq. 11.

Thermo-mechanical model: In many cases, it is more convenient to write the governing equations of thermoelasticity in terms of stress tensor. The governing equations of thermoelasticity in terms of displacement components and in the absence of body force are (Timoshenko and Goodier, 1982).

\[ \mu \{ u_{ab} \} + (\lambda + \mu) \{ u_{ka} \} - (\lambda + 2\mu) \alpha T_j = \rho \{ u_t \} \]  
\[ (16) \]

where, \( \lambda \) and \( \mu \) are Lame constants, \( \alpha \) is thermal expansion coefficient, \( u \) is displacement vector, and another term of \( u \) is derivation from displacement. Using the strain-displacement relation, \( \varepsilon_{ij} = 1/2(u_{ij} + u_{ji}) \) and substituting into Eq. 16 yields:

\[ \mu \{ \varepsilon_{ab} \} + (\lambda + \mu) \{ \varepsilon_{ka} \} - (\lambda + 2\mu) \alpha T_j = \rho \varepsilon_{ij} \]  
\[ (17) \]

Substituting for strain from stress-relations:

\[ \varepsilon_{ij} = \frac{1}{2\mu} \left( \sigma_{ii} - \frac{\lambda}{\lambda + 2\mu} \sigma_{kk} \delta_{ij} \right) + \alpha (T - T_0) \delta_{ij} \]  
\[ (18) \]

where, \( T - T_0 \) is the temperature change and \( \delta \) is kronecker delta.

The generated heat in a welding process will dissipate from the welding zone by thermal radiation, conduction, and convection. Radiation losses are dominating at high temperatures near and in the weld zone and convection has a major role at low temperatures away from the weld zone. To take into account these two effects, a total temperature-dependent heat transfer coefficient has been used (Yaghi et al., 2006):

\[ h = \begin{cases} 0.0668 T(W/m^2 \cdot ^\circ C) & \text{When } 0 \leq T \leq 500^\circ C \\ 0.231 T - 82.1(W/m^2 \cdot ^\circ C) & \text{When } T \geq 500^\circ C \end{cases} \]  
\[ (19) \]

where, \( T \) is the temperature. The above thermal boundary condition has been employed on all free boundaries of the 2-D canal. To account for the heat transfer due to fluid flow in the weld pool, an artificially increased thermal conductivity, which is several times larger than the value at room temperature, is assumed for temperatures above the melting point. The thermal effects due to solidification of the weld pool are modeled by taking into account the enthalpy. Also, the physical properties of the material have been considered as temperature dependent parameters (Table 1, 2). A bi-linear elasto-plastic formulation of the material behaviour has also been used with the tangent modulus of 5 GPa.

### 2D FE Modeling

Figure 2 shows the geometry of the rectangular canal with a vertical plate welded on its upper wall. The inner dimension of the canal is 0.2 x 0.2 m. To model the welding process, only the upper wall of the canal and the vertical plate has been considered. The geometrical dimensions of the plate and canal-wall are shown in Fig. 3. The welding itself has been carried out in three passes and they have been modeled using birth and death of elements.
techniques which are shown in Fig. 4-6. For each welding pass, the associated elements were activated and have been assumed to be at 1500°C (Welding pool Temperature of SUS304) for 1 sec. After the first pass and before starting the second pass, a time period of 120 sec has been given which indirectly allows for the welding time required to finish the electrode movement in the z direction. Also, between the second and the third pass, a time period of 80 sec has been given. The canal conveys a passing gas flow at 480°C and 2 MPa. Due to the passing of the gas flow with the velocity of 3 (m sec⁻¹), convection heat transfer in the inner side of the canal-wall has been calculated 400 (J/m²/K/sec) using (Bang et al., 2002).

\[
\frac{h_D}{k_e} = 0.025 \left( \frac{\mu_g}{\mu_{te}} \right)^{0.4} \left( \frac{C_0 h}{k_e} \right)^{0.4} \tag{20}
\]

where, \(D\) is the hydraulic diameter, \(v_g\) is the velocity of gas, \(\mu_g\) is the viscosity of the gas and \(h\) is the heat transfer coefficient between inner wall and flowing gas. Also thermal-physical data to calculate \(h\) are shown in Table 3.

Supporting boundary conditions which have been assumed based on the welding Fig-Fixture design are also shown in Fig. 3.

**Simulated results of 2-D models:** To study the effect of different parameters, three case studies have been solved:

**Basic case:** This case study simulates a simple welding process (not in-service welding). Therefore, the following assumptions or boundary-initial conditions have been considered. Initial temperature for both plates is 298 K. The convection heat transfer from plate and canal-wall surface is \(h=15\) (J/m²/K/sec). The results of analysis for each pass have been calculated and are presented below.

**First pass:** Figure 7 shows the Von-Mises stress distribution during the welding process for the first pass. It can be seen that the burn-through zone is under the weld pool at the inner side of the canal-wall. In this zone, the effective stress is larger than the yield point of the material at the associated temperature level. Also Fig. 8 shows the temperature gradient and Von-Mises stress

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**Table 3: Thermo-physical data of natural gas**

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Specific heat of gas, (C_p)</td>
<td>2245.36 (J/(\text{kg} \cdot \text{K}))</td>
</tr>
<tr>
<td>Density of gas</td>
<td>36.9 ((\text{kg} \cdot \text{m}^{-3}))</td>
</tr>
<tr>
<td>Thermal conductivity of gas, (K)</td>
<td>3.5554 (\times 10^{-2}) (J/(\text{m} \cdot \text{K} \cdot \text{sec}))</td>
</tr>
<tr>
<td>Viscosity of gas</td>
<td>1.11 (\times 10^{-2}) (Pa (\text{sec}^{-1}))</td>
</tr>
</tbody>
</table>
Burn-Through zone under welding pool

Fig. 7: Von-Mises stress distribution during the welding process for the first pass - basic case

Fig. 8: Temperature gradient, Von-Mises stress distribution and yield stress along the canal wall thickness in the direction of A-B (Fig. 7) for the first pass - basic case

Fig. 9: Temperature gradient, Von-Mises stress distribution and yield stress along the canal wall thickness in the beneath of welding pool for the second pass - basic case

distribution along the canal wall thickness in the direction of A-B which starts from the weld pool (Fig. 7). Figure 8 also shows the yield stress variation at the same direction with temperature.

It can be seen that in two zones, the stress level is larger than the yield point. At the first zone (0<x<7.5 mm), which is exactly attached to the welding pool, material has entered the plastic zone due to the high temperature. The second zone (12.5<x<15), in which the stress is larger than yield stress, can be regarded as the burn-through zone. This diagram shows that at least 30% of the wall-thickness the effective stress is below the yield point.

Second pass: After finishing the first pass, a time period of 120 sec has been given to permit the weld pool to cool down. Then, the elements associated with the second pass have been activated with constant temperature of 1500°C for one second. Figure 9 shows the temperature gradient and Von-Mises stress distribution along the canal wall thickness starting from the weld pool. Figure 9 also shows the yield stress variation at the same direction with temperature. It can be seen that the pattern of the stress and temperature variation is the same as the first pass, however, the burn-through region became larger.

This is due to the accumulation of heat and thermal stresses generated during the first pass.

Third pass: After the second pass, the welding zone is cooled down for 80 sec, then, the third pass has been started. Figure 10 depicts the temperature gradient and Von-Mises stress distribution along the canal wall thickness starting from the weld pool. In the third pass, the stress level increases in the region under the weld pool (0<x<3 mm) from 250 MPa for the second pass to 300 MPa for the third pass. This is due to the accumulation of heat and thermal stresses. However, the stress level decreases sharply immediately beyond this region and for x>5 mm the stress level is below the yield limit and therefore there isn’t any risk of burn-through. This can be justified by knowing that during the third pass, the filler weld material is added to the vertical plate rather than the canal wall (compare Fig. 4-6). Also, as it...
can be seen in Fig. 10, the temperature gradient in the lower part of the canal-wall (x>5 mm) decreases significantly comparing with those for the first or second passes.

**First case:** This case is a more realistic simulation of an in-service welding process. A fluid with static pressure of 2 MPa and 298 K has been introduced in the main canal. Therefore, a convection heat transfer of $h=15$ (J/m$^2$K/sec) (for almost zero fluid velocity) has been considered on the inner surface of the main canal. All of the other boundary conditions are the same as the basic case study.

**First pass:** The Von-Mises stress distribution during the welding process for the first pass is shown in Fig. 11. It can be seen that the location of the burn-through zone is similar to the one observed in the basic case study and it is located under the welding pool at the inner side of the canal-wall. In this zone, the effective stress is larger than the yield point of the material at the associated temperature level. Also, the temperature gradient and Von-Mises stress distribution are shown in Fig. 12 along the canal wall thickness in the direction of A-B which starts from the weld pool Fig. 11. Comparison of the stress distributions for two case studies (presented in Fig. 8 and 12) shows that the value of the Von-Mises stresses has
been decreased in the first case. This is because of the tensile stresses induced by the existing internal pressure of the main canal which is in the opposite direction of the thermal stresses produced during the welding process.

**Second pass:** Figure 13 shows the temperature gradient and Von-Mises stress distribution along the canal wall thickness during the second pass starting from the weld pool. Figure 13 also shows the yield stress variation at the same direction with temperature.

**Third pass:** After the second pass, the welding zone is cooled down for 80 sec; then, the third pass has been started. Figure 14 depicts the temperature gradient and Von-Mises stress distribution along the canal wall thickness starting from the weld pool.

The comparison of the results obtained for the first case (at the presence of internal pressure of 2 MPa along the inner canal) with those obtained for the basic case study shows that the stress distributions and their pattern are almost similar and this is due to the insignificant amount of the internal pressure relative to the induced thermal stresses.

**Second case:** This case in fact is the realistic simulation of the in-service welding process. In this case, the high pressure-hot fluid flow has been assumed to pass through the main canal with convection heat transfer of \( h=400 \text{ (W/m}^2\text{K/sec)} \), pressure of 2 MPa and temperature of 735 K. The high heat convection inside the canal is because of the high velocity gas flow in the canal (3 m sec\(^{-1}\)). Because of this hot fluid flow, the initial temperature of the main canal has been increased to the steady state temperature of 623 K. However, other thermal and mechanical boundary conditions remain the same as the previous case study: initial temperature of upper plate is 298 K and the convection heat transfer from this plate and outer side of the wall is \( h=15 \text{ (W/m}^2\text{K/sec)} \).

**First pass:** Figure 15 shows the Von-Mises stress distribution during the welding process for the first pass. It can be seen that the burn-through zone is under the weld pool at the inner side of the canal-wall. In this zone, the effective stress is larger than the yield point of the material at the associated temperature level. Figure 15 shows that the size of burn-through zone is larger comparing with those observed in earlier case studies. This is due to the high temperature of the main canal wall and the applied mechanical pressure.
Fig. 16: Type of the supports in the third case-A

To reduce the risk of the burn-through, the mechanical boundary conditions have been changed in order to give more relaxed state to the structure to accommodate the thermal expansion/deformations. The new defined boundary conditions are shown in Fig. 16.

**Third case-A:** In this case, it has been tried to decrease the effect of the mechanical supports on the thermal stresses induced by welding. The results of the this case study show that the risk of the burn-through has been decreased significantly.

**First pass:** The Von-Mises stress distribution during the welding process for the first pass is shown in Fig. 17. It can be seen that the burn-through zone has been faded away under the weld pool at the inner side of the canal-wall (3<x<8 mm, x>10 mm).

Figure 17 shows that the value of the Von-Mises stresses have been decreased (comparing with those shown in Fig. 15, 12, 8). This is due to the change of mechanical supports and it shows the major role of supports in decreasing thermal stresses.

**Second pass:** The Von-Mises stress distribution during the welding process for the second pass for this case is shown in Fig. 18.

**Third pass:** Figure 19 shows that the risk of burn-through has been completely eradicated. It is due to three main reasons: heat transformation with fluid flow, the configuration of mechanical supports and the shape and location of the third bead. It can be seen that for x>4 mm (75% of the wall-thickness) the Von-Mises stresses are lower than the yield stress for the associated temperature.

**THE TEMPERATURE-TIME DIAGRAMS FOR THE THIRD CASE-A**

Apart from Re-heat cracking caused by creep damage, hot-cracking is one of the most serious problems encountered during or after welding. Due to the high thermal input or heat accumulation, especially in the in-service welding process, the temperature level goes beyond sensitization temperature, causing the chromium atoms to separate from grain boundaries and produce chromium-carbides (Fig. 20). Grains become depleted in chromium and lose their corrosion resistance. The boundaries provide a fast cracking zone and therefore, cracks appear in these grain boundaries quickly.

Cold cracking is also a major concern for welded joints. Important factors that contribute to the cold-cracking are: susceptible microstructure of high hardness, hydrogen content, carbon equivalent, tensile stresses and
time-temperature history of the weld and Heat Affected Zone (HAZ). Generally the maximum HAZ hardness is regarded as an approximate index for susceptibility to cold cracking. The temperature-time history graphs for the welding process can also give valuable information to predict the microstructure of the weld material and HAZ which in turn can be used to predict the hot or cold cracking risks. Here, the temperature-time diagrams have been presented for three points (A, B and C) on the weldment shown in Fig. 21.

The temperature-time diagrams for point A: Figure 22 shows three temperature peaks: one at $t=0$ with 870 K which corresponds to the start of the first pass of the welding. The decrease of temperature after this point is due to the rest time after finishing first pass and before starting the second pass. The second peak corresponds to the second pass of the welding ($t=120$ sec) which gives the maximum temperature for point A (1620 K). Figure 22 also shows the high rate temperature variation for point A which will have a significant effect on the microstructure of the material. The third peak corresponds to the third pass of the weld ($t=240$ sec). It can be seen that the third pass has negligible effect on the temperature history of point A.

The temperature-time diagrams for point B: Figure 23 also shows three temperature peaks which correspond to the three welding passes. First peak at $t=0$ with 1770 K which corresponds to the start of the first pass of welding. This high rate cooling or temperature variation for point B will have a significant effect on the microstructure of the material at this zone. The decrease of temperature after this point is due to the rest time after finishing first pass and before starting the second pass. The second peak corresponds to the second pass of welding ($t=120$ sec) which gives the maximum temperature for point B (1000 K). The third peak corresponds to the third pass of the weld ($t=240$ sec). It can be seen that the third pass has negligible effect on the temperature history of point B.

The temperature-time diagrams for point C: Figure 24 shows the temperature history for point C which is on the inner side of the canal-wall. Three temperature peaks
Fig. 24: The temperature-time diagrams for point C which correspond to the three welding passes are shown in Fig. 24. First peak at t=25 sec with 745 K which corresponds to the start of the first pass of welding. Although the first pass starts at t=0 sec, but its thermal wave reaches its peak level at point C after 25 sec. Also the second and third peaks occur at 150 sec and 230 sec which correspond to the second and third weld passes. It can be seen that the time delay for the third pass is only 5 sec because point C is closer to the third weld bead. Although Fig. 24 shows that welding has negligible effect on the temperature history of point C (with maximum temperature of 775 K) but this temperature should be checked against the ignition temperature of the gas passing through the canal or pipe, otherwise there is a risk of explosion during the welding process.

DISCUSSION AND CONCLUSIONS

In this study, 2-D FE models have been developed to analyse the temperature fields and Von-Mises stress distributions for SUS304 stainless steel during the in-service welding of a canal carrying hot-pressurised gas. Although in the earlier studies, burn-through has been modeled numerically, but in this study, the effect of different parameters on the burn-through such as boundary conditions has been taken into account. Also the time temperature graphs have been provided which can be used to check the cracking of the weldment. Therefore, burn-through has been investigated in four cases to study the effect of different parameters. To assess the accuracy of this approach, model predictions have been compared against burn-through prevention information generated by API recommended for in-service welding. According to this recommendation, for pipes or canals with wall-thicknesses greater than 12.7 mm (1/2 inch), burn-through is not a primary concern. Where burn-through is of concern, care should be taken by avoiding the use of excessive welding current.

According to the simulated results, we can draw the following conclusions:

- The results of all analysis show that the value of stresses become almost maximum under the welding pool and this results comply with API description of burn-through.
- The results of all case studies show that the risk of burn-through is high during the first and second pass of welding and this risk reduces in the third pass.
- The geometry of welding pass (weld bead) has a major role in the occurrence of burn-through. In fact, the shape and location of the bead can be arranged to reduce the thermal concentration on the wall of the canal. This effect can be seen in the results for the third pass of the welding.
- The results show that during the welding process, in some parts of canal wall, the thermo-mechanical stresses become larger than the yield stress at the associated temperature. However, in all cases, there is a part in which the stresses are below the yield point and therefore, there is no risk of burn-through. This was expected based on the API recommendations.
- The results also show the importance of the supports which can cause extra thermo-mechanical stresses. Therefore, proper design of supports can reduce the risk of burn-through.
- The results show that, if the supports have been designed properly, the value of stresses will be decreased even if the internal pressure has been applied. This is because of the tensile stresses induced by the existing internal pressure of the main canal which may be the opposite direction of the thermal stresses produced during the welding process.
- In the realistic model of the in-service welding, passing of hot fluid through the canal has caused a decrease in stress values. This is because of the high cooling rate that decreases the risk of burn-through. But on the other hand, the temperature-time diagrams show that the temperature may become larger than the sensitization temperature of SUS304 stainless steel and this increases the probability of cracking risk. It is necessary that we achieve the optimum value of cooling rate to control both burn-through and cracking risks.
- Although the results show that welding has negligible effect on the temperature history of point C (on the inner side of the main canal-wall) but this temperature should be checked against the ignition
temperature of the gas which passes through the
canal or pipe, otherwise there is a risk of explosion
during the welding process.

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