Influence of Girth Welding Material on Thermal and Residual Stress Fields in Welded Lined Pipes

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ABSTRACT

An experimental and numerical investigation on the thermal and mechanical response of a lined pipe (compound pipe) under welding is presented. The welding process consists of a single-pass overlay welding (inner lap-weld) and a two-pass girth welding (outer butt-weld). The influence of the filler material of the girth welding has been examined thermally and mechanically as it is a key factor that can affect the quality of lined pipe welding. To this end, a three-dimensional non-linear finite element model based on the ABAQUS code has been developed and successfully validated against small-scale experimental results. This study was conducted on two specimens of lined pipe joined together by a girth welding deposited either by mild steel or by austenitic stainless steel. Furthermore, in this study, a pre-heat treatment required to produce lined pipe specimens has been taken into account. Strains and residual stresses have been measured by means of high temperature strain gauges, residual stress gauges and the X-ray diffraction technique along the inner and outer surfaces of the welded lined pipe whereas the thermal history has been recorded by thermocouples. The findings point out that replacing the girth welding mild steel by austenitic stainless steel has a significant effect on the residual stress results but no influence on the thermal history results.

Keywords: Lined pipe; Weld overlay; Girth welding; Thermal history; Strain; Residual stress

1. Introduction

Welding is widely used in the Oil & Gas industry to join segments of pipelines together with a high strength bond. Harsh operating conditions and corrosive production fluids often make the use of carbon-manganese (C-Mn) steel pipes impossible, and hence, alternatives are
required. Furthermore, for hydrocarbon pipelines where the transported crude oil is corrosive with such impurities as H$_2$S or CO$_2$, C-Mn steel pipes are not feasible. Cladding the C-Mn pipe with a thinner inner layer, which is resistant to corrosion, is a feasible alternative option, so called lined pipe [1, 2]. In this case, the inner layer (the liner) protects the outer pipe (backing steel) from corrosion. The liner is made of a corrosion resistant alloy (CRA) such as austenitic stainless steel, whilst the thicker outer pipe is made of low carbon steel, such as C-Mn steel, to maintain the mechanical strength of the lined pipes [3].

For several years, great efforts of Karlsson and Josefson [4] had been devoted to develop full 3D thermo-mechanical models for a circumferential single-pass butt welding in a C-Mn pipe. To achieve that, the Finite Element (FE) code ADINAT/ADINA was compared against their previous experimental work [5-7]. Subsequently, Brickstad and Josefson [8] had developed a circumferential multi-pass welding in stainless steel pipe using the FE code ABAQUS. Song et al. [9] proved that the increase in number of pipe girth welding passes does not have a significant influence on the change in residual stresses. A full 3D FE model is preferable when the number of weld passes is less than five [10]. Furthermore, the details of 3D thermal history and residual stress distributions could not be recorded by the 2D analysis when the Gaussian distribution of the heat source has been applied [11].

In the last two decades, FE codes have attracted much attention which in turn leads to high flexibility in simulating welded pipes. Velaga and Ravisankar [12] developed a 3D model to investigate the effect of welding pool geometric parameters on the thermal and residual stress distributions in austenitic stainless-steel welding. The validated numerical results point out that there is a marginal effect on thermal fields whereas no influence on the residual stress fields. The influence of thermal processes including cladding, buttering, heat treatment and dissimilar butt-welding between austenitic stainless-steel specimens and ferritic steel specimens have been investigated by Dehaghi et al. [13]. The findings show that residual stresses induced by these thermal factors are insignificant because the high welding temperature removes inherent residual stresses. Charkhi and Akbari [14] examined the influence of the preheating process on the residual stresses in the repair welding of stainless steel and carbon steel pipes. Their results highlight that the preheating has a lower influence on reducing the residual stresses in the stainless steel pipe than that in the carbon steel pipe due to the effect of thermal and mechanical properties. Lee and Chang [15] carried out a comparative study between girth-welded austenitic and duplex stainless steel pipes using a 3-D FE analysis to evaluate the residual stresses. The results show that the residual stresses are
larger in the austenitic stainless steel pipes because of relatively higher thermal expansion at elevated temperatures. Deng et al. [16] have investigated the effect of yield strength of weld metal and of the strain hardening on the residual stress distributions in a SUS304 steel multi-pass butt-welded joint. Their findings prove that both factors have a significant influence on the residual stresses, but they have insignificant influences on the equivalent plastic strain distributions. Recently, some studies discussed through-thickness residual stresses of dissimilar welded joints identifying positive influences in reducing through-thickness residual stresses, such as post welding heat treatment and buttering layer through thickness [17, 18].

There are very few studies that can be found in the literature that address lined pipe modelling associated with simulation of both the weld overlay and girth welding. Obeid et al. [19-21] developed a new procedure to simulate the lined pipe welding using FORTRAN subroutines and ABAQUS code. Their studies find out that the halving of the weld overlay and girth welding speeds leads to increases the absolute values of residual stresses at the welding centreline whereas doubling the welding speeds do not have that effect [19]. The results prove that the use of a liner has a considerable effect on the thermal and residual stress fields [20]. Furthermore, a parametric study had been conducted to examine the influence of various factors on the residual stresses [21].

In this study, a sensitivity analysis to determine the influence of the girth welding material in the welded lined pipe has been numerically and experimentally investigated. To this end, thermal and mechanical FE models have been developed using FORTRAN subroutines and ABAQUS code for two cases. In the first case, described here as Case A or reference case, the weld overlay is deposited by austenitic stainless-steel whilst the girth welding is deposited by mild steel. In Case B, the deposited material of girth welding is the same deposited material utilised in weld overlay, namely austenitic stainless steel. Austenitic stainless steel is widely used in industry as a welding material to join carbon steel specimens together because it has a better corrosion resistance, thermal and mechanical properties than mild steel welding material. The FE model is also executed to study the thermal and mechanical effect of the pre-heat treatment procedure, known as Tight Fit Pipe (TFP), to insert the liner inside the C-Mn pipe. Based on thermal and stress fields, this paper addresses the best filler material choice, mild steel or austenitic stainless steel, for lined pipe girth welding with considering the influence of TFP process. The numerical strains and stresses were compared against their recorded counterparts using various methods such as residual stress gauges, high temperature strain gauges and X-ray diffraction.
2. Manufacturing process and welding

In this work, the test segment, schematically sketched in Fig. 1, is composed of two welded identical segments of lined pipes. In particular, the backing pipe is made of Carbon-Manganese (C-Mn) steel equivalent to low carbon steel AISI 10305. To maintain the mechanical integrity, the backing pipe has a significant wall thickness with 6.35 mm where the outer diameter is 114.3 mm. The inner liner is made of austenitic stainless steel equivalent to AISI 304 which has a relatively considerable content of Chromium (Cr) and Nickel (Ni) as tabulated in Table 1. To minimize the cost of lined pipe, the inner liner has a thinner wall thickness with 1.5 mm where its outer diameter is 98.6 mm. The whole length of the test segment is 400 mm which is symmetric about the mid plane, welding centreline WCL.

Fig. 1 Two identical welded lined pipes connected by single-pass weld overlay and two-pass girth welding, dimensions in mm.

<table>
<thead>
<tr>
<th>Grade</th>
<th>C %</th>
<th>Mn %</th>
<th>Cr %</th>
<th>Ni %</th>
<th>P %</th>
<th>S %</th>
<th>Al %</th>
<th>Si %</th>
</tr>
</thead>
<tbody>
<tr>
<td>AISI 304</td>
<td>≤ 0.08</td>
<td>≤ 2</td>
<td>18-20</td>
<td>8-10.5</td>
<td>≤ 0.045</td>
<td>≤ 0.03</td>
<td>-</td>
<td>≤ 1</td>
</tr>
<tr>
<td>AISI 10305</td>
<td>≤ 0.17</td>
<td>≤ 1.2</td>
<td>-</td>
<td>-</td>
<td>≤ 0.045</td>
<td>≤ 0.045</td>
<td>≥ 0.02</td>
<td>≤ 0.35</td>
</tr>
</tbody>
</table>

The TFP pre-heating process, which mainly depends on expanding the outer pipe by heating and shrinking the inner one by cooling down, is performed to insert the austenitic stainless steel pipe inside the low carbon steel pipe. The detailed TFP process is discussed later. Post the heating process, each identical segment is naturally cooled down to room temperature. Then, each section is machined using a CNC machine by cutting 3 mm from one end of the
inner pipe and chamfering the same end with a 30° degree on the outer pipe, Fig 1 refers. Afterwards, the two identical sections are set on the same alignment after filling the cut-off ends of the liner with weld overlay. The butt welding, girth welding, is deposited after that in the V-groove to assemble the two segments together. Attention is paid to avoid any misalignment between the sections, which can intensify the stress at the connected regions. The welding process is composed of one-pass weld overlay (lap-weld) and two-pass girth welding (butt-weld). Tungsten Inert Gas (TIG) welding is used to deposit the weld overlay at the cut ends of the inner layer using ER308 stainless steel rod. Likewise, TIG welding is executed to deposit the two-pass girth welding after the single-pass weld overlay to join the two identical segments of lined pipe together using E70S2 mild steel rod in Case A whilst the ER308 stainless steel rod is used in Case B.

To undertake the welding process, the heat torch is fixed in a particular position whilst the two lined pipe segments turn with a constant speed for each pass. The weld overlay is deposited through one pass which needs 240 s to turn 360°. Subsequently, the lined pipe is left to cool down naturally for 270 s, which is the inter-pass time, to reach 100°C. Then, the welding is resumed to execute the first girth welding in 270 s and to take after that another inter-pass for 270 s again. The second-pass girth welding is resumed to be deposited in 270 s over the first-pass girth welding in the V-groove. After the welding process, the entire welded lined pipe is left for 3000 s to cool down in natural air to the room temperature. The temperatures and strains are recorded during the whole welding process including the single-pass weld overlay and the two-pass girth welding as depicted in Fig. 2.

During the three passes, the heat source starts moving circumferentially anti-clockwise from $\Theta = 0^\circ$ and heading forward thereafter to complete one pass by arriving at the same location of the starting central angle which is $\Theta = 360^\circ = 0^\circ$. 
Fig. 2 Recording the thermal and mechanical results during welding

High temperature K-type thermocouples, HI-766F, were used to measure the temperature at 6 positions in proximity to the fusion zone as sketched in Fig. 3. Half of them were attached on the outer surface (AISI 10305) on the same alignment with a central angle of 180°. The remaining thermocouples were mounted on the same horizontal line with 180° central angle on the inner stainless steel surface (AISI 304 liner). The thermal histories were recorded during welding and natural cooling to reach the room temperature by means of LabVIEW software compatible with 24-bit A/D interface (NI 9213).

Fig. 3 Recording thermal histories by thermocouples, locations in mm.

Along with recording the thermal history, the strain evolution during welding and natural cooling was recorded through six 1-axis, ZFLA-11, and three 2-axis strain-gauge, ZFCAL-17, strain gauges. The six 1-axis strain gauges were attached on the outer surface (AISI
10305 pipe), whilst the 3 biaxial strain gauges were attached on the inner surface (AISI 304 liner). The strain history was recorded longitudinally and circumferentially through LabVIEW set (NI 9213).

To measure the residual stresses after welding and natural cooling of the welded pipe to ambient temperature, 20 °C, 14 tri-axial residual stress gauges, FRS-2, were also attached on the outer surface (AISI 10305) and on the inner surface (AISI 304) of the lined pipe as shown in Fig. 4. The residual stress results were also obtained by LabVIEW. According to ASTM-E837, the recorded results were obtained by gradually drilling a small hole through the thickness with both depth and diameter of 2 mm. To increase the reliability of residual stress results, an X-Ray diffraction technique was subsequently utilised to verify the results obtained by gauges.

Fig. 4 Tri-axial hole-drilling residual stress gauges, FRS-2.

3. Thermo-mechanical analyses

The thermal and mechanical simulations have been developed using ABAQUS code [23]. The lined pipe is symmetric around the mid-plane which is located at the Weld Centreline (WCL). Therefore, only one segment of welded lined pipe has been modelled in order to reduce the time consumption. The mechanical properties such as yield stress, Young’s modulus and Poisson’s ratio are inversely affected by the temperature. The thermal properties
such as specific heat, conductivity and thermal expansion are not affected with the mechanical deformation. Therefore, the thermal analysis is executed first to obtain the thermal history at each node of the welded lined pipe elements with respect to time. In the thermal analysis, each node possesses 1 DOF, that is the temperature, where each element, named DC3D20 in ABAQUS code, is composed of 20 nodes. The thermal loads are transferred from the thermal analysis to the mechanical analysis for each step accordingly where both analyses have the same solid body with identical mesh composed of nodes and elements. In the mechanical analysis, each node has three translation degrees of freedom where each element is composed of 20 nodes, named C3D20 in ABAQUS code. In both Cases A and B, the models have the same mesh associated with the same number of nodes and elements, 35220 and 7380, respectively. Due to the higher temperature, a fine mesh is deployed at the fusion zone (FZ) and at the heat affected zone (HAZ). As illustrated in Fig. 5, the weld overlay, the AISI 304 liner, the girth welding and AISI 10305 pipe are coloured with white, green, blue and red, respectively.

![Fig. 5 Solid body of lined pipe model](image)

Apart from the yield points, the parent and weld materials have similar thermal and mechanical properties, with the weld material having higher yield points for both materials, AISI 10305 and AISI 304, as tabulated in Tables 2 and 3, respectively. The initial temperature on the entire lined pipe is room temperature. The melting point for AISI 10305 is
and AISI 304 is set to be 1500 °C and 1365 °C with the associated latent heat set to be 247 kJ/kg and 260 kJ/kg, respectively.

Table 2 Thermal and mechanical properties of AISI 10305 [4].

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>Density (kg/m³)</th>
<th>Specific heat (J/kg °K)</th>
<th>Conductivity (W/m °K)</th>
<th>Thermal expansion (x10⁻⁵ °K⁻¹)</th>
<th>Yield stress (MPa) Parent</th>
<th>Yield stress at 1% hardening (MPa) Parent</th>
<th>Young’s modulus (GPa)</th>
<th>Poisson’s ratio</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>7860</td>
<td>444</td>
<td>50</td>
<td>1.28</td>
<td>349</td>
<td>445</td>
<td>522</td>
<td>210</td>
</tr>
<tr>
<td>100</td>
<td>480</td>
<td>48.5</td>
<td>1.28</td>
<td>331</td>
<td>441</td>
<td>405</td>
<td>515</td>
<td>200</td>
</tr>
<tr>
<td>200</td>
<td>503</td>
<td>47.5</td>
<td>1.30</td>
<td>308</td>
<td>417</td>
<td>379</td>
<td>482</td>
<td>200</td>
</tr>
<tr>
<td>300</td>
<td>518</td>
<td>45</td>
<td>1.36</td>
<td>275</td>
<td>376</td>
<td>341</td>
<td>425</td>
<td>200</td>
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<tr>
<td>400</td>
<td>555</td>
<td>40</td>
<td>1.40</td>
<td>233</td>
<td>325</td>
<td>291</td>
<td>375</td>
<td>170</td>
</tr>
<tr>
<td>600</td>
<td>592</td>
<td>35</td>
<td>1.52</td>
<td>119</td>
<td>173</td>
<td>159</td>
<td>200</td>
<td>56</td>
</tr>
<tr>
<td>800</td>
<td>695</td>
<td>27.5</td>
<td>1.56</td>
<td>60</td>
<td>43</td>
<td>79</td>
<td>65</td>
<td>30</td>
</tr>
<tr>
<td>1000</td>
<td>700</td>
<td>27</td>
<td>1.56</td>
<td>13</td>
<td>14</td>
<td>20</td>
<td>32</td>
<td>10</td>
</tr>
<tr>
<td>1200</td>
<td>700</td>
<td>27.5</td>
<td>1.56</td>
<td>8</td>
<td>9</td>
<td>14</td>
<td>16</td>
<td>10</td>
</tr>
<tr>
<td>1400</td>
<td>700</td>
<td>35</td>
<td>1.56</td>
<td>8</td>
<td>9</td>
<td>13</td>
<td>16</td>
<td>10</td>
</tr>
<tr>
<td>1600</td>
<td>700</td>
<td>122.5</td>
<td>1.56</td>
<td>8</td>
<td>9</td>
<td>9.5</td>
<td>16</td>
<td>10</td>
</tr>
</tbody>
</table>

To simulate the practical welding behaviour, the element birth technique was implemented to simulate the flow of welding material in its groove. All the welding elements are removed first. The welding pool represented by a set of elements is reactivated once the heat torch arrives. The welding pools are reactivated sequentially replicating real welding. During reactivation of the specified welding pool, thermal conductivity, specific heat and the other material properties of the elements within the pool are ramped up by multiplying their value by a scaling factor changing from zero to one [26].

3.1. Thermal analysis

3.1.1. Tight fit pipe process
In this work, an innovative procedure, based on TFT process, has been developed to manufacture lined pipes in a laboratory. The procedure consists of sequential steps listed as follows:

1. Heating the C-Mn pipe inside the furnace up to 500 °C.
2. Cooling the AISI 304 liner with liquid nitrogen down to -200 °C.
3. Inserting the liner carefully and smoothly inside the C-Mn pipe inside the furnace taking the risk assessment into consideration.
4. Taking the lined pipe away from the furnace to be naturally cooled down to room temperature in 7200 s as depicted in Fig. 6.

![Fig. 6 The manufactured pipes by TFP.](image)

3.1.2. Thermal behaviour during welding

Throughout the TFP treatment, the whole lined pipe is subject to a series of heat exchanges along its external surfaces exposed to the environment depending on Newton’s law of thermodynamics [19]. During the welding process, the effect of radiation and convection is dominant on the entire lined pipe surfaces. The radiation effect, $q_{rad}$, is dominant in the FZ and HAZ whilst the convection influence, $q_{conv}$, is dominant in the zones apart from the
latter ones. In this case, the total heat transfer coefficient takes into account the effect of radiation and convection presented as Eq. (1) [21]:

\[ h_{total} = h_{conv} + \sigma_{em} \varepsilon_{bol}(T_{pipe} + T_{air})(T_{pipe}^2 + T_{air}^2) \]  

(1)

where \( h_{total} \) is the total heat-transfer coefficient, \( h_{conv} \) is the convective coefficient, \( \sigma_{em} \) is the effective emissivity, \( \varepsilon_{bol} \) is the constant of Stefan-Boltzmann, \( T_{pipe} \) is the surface temperature and \( T_{air} \) is the air temperature.

The lined pipe mainly consists of two pipes made of AISI 304 and AISI 10305. In this case, each material has its own heat transfer parameters at ambient temperature, as shown in Table 4.

Table 4 Parameters of heat exchange for AISI 10305 and AISI 304

<table>
<thead>
<tr>
<th>Parameters</th>
<th>AISI 10305</th>
<th>AISI 304</th>
</tr>
</thead>
<tbody>
<tr>
<td>( h_{conv} ) (W/m² K)</td>
<td>8 [15]</td>
<td>5.7 [15]</td>
</tr>
<tr>
<td>( \sigma_{em} )</td>
<td>0.51</td>
<td>0.75</td>
</tr>
<tr>
<td>( \varepsilon_{bol} ) (W/m² K²)</td>
<td>5.67×10⁻⁸</td>
<td>5.67×10⁻⁸</td>
</tr>
</tbody>
</table>

To apply the heat exchange upon the AISI 10305 and AISI 304 surfaces exposed to the environment, a FILM user-subroutine [23] has been implemented to code the total heat-transfer coefficient, that is Eq. (1), using FORTRAN.

To apply the heat source in the weld zones, a DFLUX user-subroutine has also been carried out in ABAQUS using FORTRAN code. In particular, the heat source is coded as a Gaussian function where its location depends on the time to circumferentially move an ellipsoidal welding pool from the initial centre \((x_0, y_0, z_0)\) [19]:

\[ q(x, y, z, t) = \frac{6Q \sqrt{3}}{mnln\pi\sqrt{\pi}} e^{-3(x-(Lsin\theta+x_0))^2/m^2} e^{-3(y-(Lcos\theta+y_0))^2/n^2} e^{-3(z-z_0)^2/l^2} \]  

(2)

where \( Q = IV\mu \) is the heat input composed of the current \( I \), voltage \( V \) and the weld efficiency \( \mu \), \( L \) is the distance between the torch position and the central axis, \( \theta \) is the central angle. Variables \( m, n \) and \( l \) are the local semi-axes of the ellipsoidal welding pool in directions, \( x, y \) and \( z \), respectively, as shown in Fig. 7. To have uniformly distributed heat source, the parameters \( m \) and \( l \) must be equal to or greater than the length and width of the element set of the welding pool, respectively. \( n \) is the controlling parameter to adjust the uniformity of heat source for all circumferential welding pool sets.
Table 5 presents the values of the variables used to code the heat torch equation, Eq. (2), for each welding pass.

Table 5 Welding variables for each pass.

<table>
<thead>
<tr>
<th>Variables</th>
<th>Symbol</th>
<th>Weld overlay</th>
<th>1st pass girth welding</th>
<th>2nd pass girth welding</th>
</tr>
</thead>
<tbody>
<tr>
<td>Welding current (A)</td>
<td>$I$</td>
<td>242</td>
<td>485</td>
<td>515</td>
</tr>
<tr>
<td>Voltage (V)</td>
<td>$V$</td>
<td>10</td>
<td>10</td>
<td>10</td>
</tr>
<tr>
<td>Welding efficiency</td>
<td>$\mu$</td>
<td>70%</td>
<td>70%</td>
<td>70%</td>
</tr>
<tr>
<td>Welding speed (mm/s)</td>
<td>$v$</td>
<td>1.3</td>
<td>1.26</td>
<td>1.33</td>
</tr>
<tr>
<td>Half-length of arc (mm)</td>
<td>$m$</td>
<td>4.9</td>
<td>6.3</td>
<td>6.3</td>
</tr>
<tr>
<td>Depth of arc (mm)</td>
<td>$n$</td>
<td>1.5</td>
<td>2.63</td>
<td>2.85</td>
</tr>
<tr>
<td>Half-width of arc (mm)</td>
<td>$l$</td>
<td>4.9</td>
<td>5.57</td>
<td>5.66</td>
</tr>
</tbody>
</table>

3.2. Structural analysis

Excluding the mechanical boundary conditions and element type, the FE mesh in the mechanical analysis must be identical to the thermal analysis in order to transfer the thermal loads to their counterparts of nodes and elements. The boundary conditions have significant impact on the numerical mechanical results. Consequently, just two nodes at the far lined pipe end are restrained radially and laterally to prevent the movement of lined pipe body. At the welding centreline (WCL) plane, all nodes are fixed axially due to the symmetry whilst free to move in other directions as illustrated in Fig. 8. A geometrically non-linear formulation is applied to allow the bending deformations, with the automatic time stepping algorithm implemented in ABAQUS.
The total strain is mainly composed of three components, which are elastic, plastic and thermal strains, neglecting the effect of other components related to the solid-state phase transformation, Eq. (4) [20].

\[ \dot{\varepsilon} = \dot{\varepsilon}^e + \dot{\varepsilon}^p + \dot{\varepsilon}^t \]  

where \( \dot{\varepsilon}^e \) is the elastic strain; \( \dot{\varepsilon}^p \) is the plastic strain and \( \dot{\varepsilon}^t \) is the thermal strain. The isotropic Hooke’s Law with respect to temperature-dependent Young’s modulus and Poisson’s ratio is applied to calculate the elastic strain. To calculate the plastic strain, a rate-independent elastic-plastic constitutive equation with respect to the Von Mises yield surface, temperature-dependent mechanical properties and hardening law is used [21]. Due to thermal cycling during welding, both materials AISI 10305 and AISI 304 are subject to a bilinear kinematic hardening rule. The hardening parameter is obtained when the plastic strain of AISI 10305 and AISI 304 is 1% [27, 28] as tabulated in Tables 2 and 3. Based on the temperature-dependent coefficient of thermal expansion, the thermal strain is calculated at each integration point in the FE model.

It is worth noting that the austenitic transformation starts at \( A_{C1} \) and terminates at \( A_{C3} \) where grain sizes become smaller. \( A_{C1} \) is a cementite disappearance temperature whereas \( A_{C3} \) is an \( \alpha \)-ferrite disappearance temperature. Rapid cooling for the welding process leads to an increase in the grain sizes which correspondingly increases the yield stress [29]. As a result, some post weld heat treatments such as annealing are required to reduce the grain size
without pre-existing residual stresses. For convenience in FE modelling, phase transformations and post weld heat treatments are neglected.

**4. Thermal and Mechanical responses**

**4.1. Thermal response**

Typical welding requires essential conditions such as the temperature and extent of FZ and HAZ to be determined [30]. To simulate practical welding, all the nodes in the welding zones should minimally reach the melting point, that is 1365 °C and 1500 °C for weld overlay (austenitic stainless steel) and girth welding (mild steel), respectively. In addition, the HAZ should extend 2-3 mm away from the FZ to reach a temperature not higher than Ac3 in the Iron-Carbon phase diagram, that is about 800 °C, as clearly illustrated in Fig. 9. To validate this, a macroscopic examination of a cross section in Case A, at 180° central angle was conducted to clearly determine the FZ and HAZ boundaries as shown in Fig. 10.

[Fig. 9 The numerical FZ and HAZ isotherms during welding at 180° (Case A)]

[Fig. 10 Micro-structure cross section at 180° (Case A).]

Furthermore, all nodes situated on the same circular line must have the same thermal record. To check that on the FE models, the computed thermal histories at central angles of 90°, 180° and 270° during the weld overlay in Case A are plotted in one graph. From Fig. 11, one may note that the peak of the thermal distribution, 1634 °C, is located at the centre of the weld overlay groove. This peak temperature is higher than the melting temperature of austenitic stainless steel AISI 304, 1365 °C. In addition to that, all nodes located in the weld overlay elements, pass the melting point. For the second conditions, it is clear that the welding pool centres at different central angles, 90°, 180° and 270°, have an identical trend and thermal history during the weld overlay.
Likewise, the thermal distributions at the welding pool centre have identical trend during depositing the second-pass girth welding at 90°, 180° and 270° in Case A. It can be seen from Fig. 12 that the three curves have the same peak temperature, 2076°C, which is higher than the melting point of C-Mn, 1500°C.

To show the thermal behaviour during the TFP process and welding, the thermal history of two points has been tracked. The first point, N-inner, is placed between two areas on the inner surface, the weld overlay and the liner (AISI 304). The second one, N-outer, is also positioned between two areas on the outer surface, the girth welding and the C-Mn pipe (AISI 10305), as depicted in Fig. 13.
Once putting the cold AISI 304 pipe inside the heated AISI 10305 pipe, the inner point, N-inner, needs 4.30 s to increase from its initial temperature, which is the liquid nitrogen temperature -200 °C, to sharply reach the balance temperature, 419 °C, with the outer point, N-outer, heated in a furnace with 500 °C. After 7200 s from inserting the AISI 304 liner inside the AISI 10305 pipe, the temperatures of both points cool down to room temperature, 20 °C.

Fig. 14(a)-(f) compares the numerical thermal results against the experimental ones recorded at six locations, TC1-TC6, during the three welding passes. One may note from Fig. 14(a)-(f) that the maximum variation between the predicted and recorded peak temperature results in both cases is less than 3% which shows good agreement. Furthermore, higher peak temperatures lead to relatively larger cooling rate where the peak temperatures reduce by moving farther away from the welding centre line.
Since the same heat input of heat source is applied in Cases A and B, the numerical and recoded thermal fields in Case A are, in large extent, close to their counterparts in Case B. One may note from Tables 2 and 3 that the thermal properties of both parent materials, AISI 10305 and AISI 304 are somewhat similar at high temperatures. Consequently, the peak temperatures during the weld overlay and girth welding passes must not be significantly affected with changing the girth welding material to austenitic stainless steel.
4.2. Mechanical response

4.2.1. Recorded strain

Fig. 14 Thermal history recorded at thermocouples (a) TC1 (b) TC2 (c) TC3 (d) TC4 (e) TC5 (f) TC6.
As with recording the temperatures during welding, recording the strain history is also necessary to give a comprehensive image to the mechanical behaviour of the lined pipe during welding. To undertake this, 12 high temperature strain gauges were used where six of them were attached on the inner surface and the remaining gauges were placed on the outer surface. Table 6 clarifies the positions of the high temperature strain gauges.

<table>
<thead>
<tr>
<th>Gauge</th>
<th>Recorded Strain</th>
<th>Location ((\Theta^\circ, Z\text{ (mm)}))</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>Axial (outer)</td>
<td>(45°, 14)</td>
</tr>
<tr>
<td>B</td>
<td>Hoop (outer)</td>
<td>(36°, 14)</td>
</tr>
<tr>
<td>C</td>
<td>Axial (outer)</td>
<td>(270°, 14)</td>
</tr>
<tr>
<td>D</td>
<td>Hoop (outer)</td>
<td>(261°, 14)</td>
</tr>
<tr>
<td>E</td>
<td>Axial (outer)</td>
<td>(135°, 18)</td>
</tr>
<tr>
<td>F</td>
<td>Hoop (outer)</td>
<td>(126°, 18)</td>
</tr>
<tr>
<td>G</td>
<td>Axial (inner)</td>
<td>(45°, 14)</td>
</tr>
<tr>
<td>H</td>
<td>Hoop (inner)</td>
<td>(45°, 14)</td>
</tr>
<tr>
<td>I</td>
<td>Axial (inner)</td>
<td>(270°, 14)</td>
</tr>
<tr>
<td>J</td>
<td>Hoop (inner)</td>
<td>(270°, 14)</td>
</tr>
<tr>
<td>K</td>
<td>Axial (inner)</td>
<td>(135°, 18)</td>
</tr>
<tr>
<td>L</td>
<td>Hoop (inner)</td>
<td>(135°, 18)</td>
</tr>
</tbody>
</table>

On the inner surface, at a longitudinal direction of \(Z = 14\) mm from the welding centreline (WCL), one biaxial rosette containing two gauges (G and H) was mounted to record the axial and hoop strains, respectively, at the circumferential angle of 45°. At the same axial distance, \(Z = 14\) mm, but with 270° central angle, another biaxial rosette (gauges I and J) was attached to record the axial and hoop strains, respectively. The remaining biaxial rosette with axial gauge K and hoop gauge L is placed at axial distance of \(Z = 18\) mm with a circumferential angle of 135°. In contrast to the inner surface, the 6 gauges used to record the strains on the outer surface were uniaxial strain gauges mounted at two axial distances, \(Z = 14\) mm and \(Z = 18\) mm, from the WCL. At \(Z = 14\) mm, uniaxial strain gauges, A and B, were mounted on the outer surface to record the hoop and axial strains with 45° and 36° central angle, respectively. At the same axial distance, the axial gauge C and the hoop gauge D were attached with circumferential angles of 270° and 261°, respectively. At \(Z = 18\) mm, the last two uniaxial gauges, E and F, were placed with 135° and 126° central angles to record the axial and hoop strain history, respectively, during welding process.

Following the welding material in the FZ produces higher temperatures which in turn soften the neighbour HAZ area, causing thermal expansion. The thermal strain was measured by placing two metal strips in a heating furnace. One strip was cut from the outer pipe whilst the second one was cut from the liner. The thermal strains were calibrated to zero first and then
recorded with gradually increasing the temperature for both strips to reach 650 °C. At 150 °C, the thermal strain gauges recorded 30 and 40 micro-strain for AISI 10305 and AISI 304 strips, respectively. Increasing the temperature to 650°C, 360 and 490 micro-strain were measured for the AISI 10305 and AISI 304 strips respectively. Consequently, pure transient mechanical strains have been recorded during lined pipe welding by deducting the thermal strain from the total recorded strains. Figs. 15 and 16 depict the transient experimental strain history against the numerical mechanical strain history for Cases A and B during welding on the outer and inner surfaces, respectively. For more convenience, just the recorded strains and temperatures in Case A have been plotted in Figs. 15 and 16.

FEA Cases A and B have been modelled to simulate the lined pipe welding process. Case A considers different materials used for weld overlay and girth welding whilst Case B takes into account the effect of using the same material, namely austenitic stainless steel, for both welds. Due to the residual stresses induced by the TFP process, that transient strain histories do not initiate from the origin, which is zero, once the welding process starts. The magnitude of this initial strain is relatively higher on the inner surface. For both Cases A and B, the numerical initial strains on the outer surface (AISI 10305) are 2.34×10^{-4}, -2.05×10^{-5}, 2.65×10^{-4}, 2.04×10^{-5}, 1.11×10^{-4} and 1.14×10^{-5} according to gauges A, B, C, D, E and F, respectively. On the inner surface (AISI 304), the initial strains are 9.1×10^{-4}, -1.7×10^{-4}, -8.7×10^{-4}, -2.2×10^{-4}, -6.9×10^{-4} and -1.1×10^{-4} according to gauges G, H, I, J, K and L, respectively.

During the weld overlay, the heat source moves ahead on the inner surface to seal the end of the liner but a sudden drop takes place in all strain gauges before the heat source arrives the particular gauge for both axial and hoop directions (e.g., Fig. 15(a), gauge A, at 15-36 seconds and Fig. 16(c), gauge I, at 155-184 seconds). The severity of this drop is different where the decrease in strain is more significant in the axial direction on the inner surface, gauges G, I and K, than that on the outer surface, gauges A, C and E, accordingly. Likewise, the recorded strains remarkably decrease at gauges H, J an I located at the hoop direction on the inner surface larger than their counterparts, gauges B, D and F, respectively, located on the outer surface. Once the heat source reaches a point located at the same axial line of a particular gauge, a noteworthy increase in strain is observed in all gauges. After passing the particular gauge, the strain reduces on the outer surface, largely again at the axial gauges, A, C and E, whilst the strain, on the inner surface, increases gradually at their counterparts, gauges G, I and L, respectively. As a consequence of this, pure bending occurs at the welding zone and its
vicinity during cooling time. One may also note a noticeable kink, particularly at gauges A and B because the heat source approaches the start/end point in their immediate vicinity. After reaching the start/end welding point at 240 s, the inter-pass time lasts 270 s to naturally cool down the welding area and its locale to 100 °C.

During the first pass of the girth welding, a sudden drop in strain signals, again, is measured in all gauges to be directly followed with a sharp strain increase as the heat torch reaches the particular gauge. Afterwards, the strain signals, in a similar way, keep increasing steadily on the inner surface whilst decreasing on the outer surface, obviously at the axial direction. The pure bending is produced again at the FZ and HAZ particularly [31]. The first-pass of girth welding is followed again with inter-pass time for 270 s. It can be seen that strain distributions recorded on the inner and outer surfaces in the hoop direction go consistently with the distributions of weld overlay discussion.

During the second pass of the girth welding, one may note that the strain signals pursue the same behaviour observed before during the weld overlay and first-pass girth welding. One further observation is that the decrease in strain magnitude is relatively larger than that in the previous welding approach, with exception of the axial strains recorded in gauges A, C and E, mounted on the outer surface.

During cooling, due to the solidification of the weld material, the weld zones shrink which in turn contribute to gradually decreasing the axial strains on the outer surface and increasing the strains on the inner surface. Under this circumstance, purely bending takes place at the weld zone and its vicinity as expected.

In general, the predicted transient strain distributions of Case A are consistent to a large extent with their counterparts in Case B at all directions and surfaces accordingly. Due to the high temperature during welding, the TFP residual stresses have been removed at the welding zones and their vicinities which in turn lead to reductions the discrepancies in strains between Case A and Case B. Moreover, as is clear from Figs. 15 and 16, the predicted strain results are in good agreement with their experimental counterparts recorded by strain gauges.
Fig. 15 Mechanical strain with temperature distributions on the outer surface for guges (a) A (b) B (c) C (d) D (e) E and (f) F.
Fig. 16 Mechanical strain with temperature distributions on the inner surface for gauges (a) G (b) H (c) I (d) J (e) K and (f) L.

4.2.2. Residual plastic strains
The key component of the total strain is the plastic strain which is mainly responsible to the permeant deformation after welding. To focus on this, Figs. 17 and 18 plot the numerical residual axial and hoop plastic strains starting longitudinally from the WCL at 180° on the inner surface, respectively, for both cases. The magnitudes of residual axial plastic strains for Cases A and B reach -0.002 and -0.001, respectively, with steady state located 24 mm away from the WCL, as illustrated in Fig. 17. It is clearly seen that both strain distributions have the same trend to a large extent along the axial direction with maximum plastic strain magnitudes of -0.22 and -0.2 for Cases A and B, respectively, located at 0.3 mm away from the WCL. The residual plastic strain distributions in the hoop direction for Cases A and B reach a value of -0.0009 at an axial distance of 37 mm from the WCL. The strain values in both cases are somewhat constant with less than zero at a distance beyond 37 mm which is influenced by the TFP process as shown in Fig. 18. It can be concluded that the influences of the welding process on axial residual stresses and hoop residual stresses in the axial direction on the inner surface therefore extend approximately 24 mm and 37 mm, respectively, for both cases.

Fig. 17 Axial plastic strain on the inner surface at 180° against the axial distance.  
Fig. 18 Hoop plastic strain on the inner surface at 180° against the axial distance.

Figs. 19 and 20 plot the numerical residual axial and hoop plastic strains, respectively, at 180° on the outer surface along the axial direction for both cases. The plastic strain on the outer surface reduces to zero at a distance beyond 24 mm and 14 mm along the axial direction, for Cases A and B, respectively, plotted in Fig. 19. The hoop plastic strains on the outer surface for Cases A and B have nearly identical distributions in the FZ and HAZ at the
central angle of 180° as shown in Fig. 20. Hoop plastic strains in Cases A and B tend to zero at 37 and 20 mm from the WCL, respectively.

Hence the influence of residual welding plastic strains axially and circumferentially extends along the axial distance of 24 mm and 37 mm from the welding centreline on the outer surface, respectively, in Case A. For Case B, the axial and hoop plastic strain limits on the outer surface extend less than that on the inner surface reaching 14 and 20 mm, respectively. Therefore, it can be concluded that the influences of the welding process on the axial and hoop residual stresses on the outer surface extend approximately 24 mm and 37 mm along the axial direction, respectively, for Case A. For Case B, the axial and hoop residual stresses on the outer surface influenced by the welding process only extend about 14 and 20 mm away from the WCL, respectively.

As forgoing comparisons, high temperatures at the FZ and HAZ on the inner and outer surfaces for both cases reduce the resistance of deformation where initial yield stresses decrease with a large predominance of strain hardening [32]. As a result, ductility and notch toughness will be influenced by the total plastic strain magnitude during welding cycles. One may note that the absolute values of the axial plastic strains are relatively larger than their counterparts of the hoop plastic strains for both Cases A and B where the intensity of boundary conditions and temperature play a key role [33]. Consequently, the length of the zone with significant hoop plastic strains is larger than that with significant axial plastic strains along the axial direction. The reason is attributed to the influence of the welding
temperatures and the intensity of the constraint in the circumferential direction (symmetric plane and lateral constraints at pipe ends). In contrast to that, the length of the zone with remarkable axial plastic strains on the inner and outer surfaces is smaller than that for hoop plastic strains due to less restraint in combination with the elevated temperatures. The thermal distributions on both surfaces for both cases are similar to a large extent. The difference between the two cases is the intensity of the constraint on the outer surface where the filler material of austenitic stainless steel is more capable to expand and shrink naturally during welding. The reason is attributed to the thermal expansion coefficient where it is larger for austenitic stainless steel. Consequently, the axial and hoop plastic strain limits affected by the welding process on the outer surface in Case A extend farther in the axial direction than those in Case B.

5. Residual stress effect

5.1. Residual stresses on the inner surface

As can be seen from Figs. 21 and 22, the predicted axial and hoop stresses on the inner surface in both cases are compared against their experimental counterparts at the central angle of 180° along the axial distance. These results take into account the effect of TFP process to manufacture the lined pipe for Cases A and B.

The peak values of both the numerical axial stresses and experimental ones in Fig. 21 are located at 0.3 and 0.6 mm away from the WCL for Cases A and B, respectively. These points are located at the toe of the girth welding. Conversely, Fig. 22 illustrates that the ultimate numerical hoop stress in Case A is located at 2.1 mm far from the welding centreline, inside the weld overlay zone. Similar to Case B in Fig. 21, the ultimate numerical hoop stress in Case B takes place exactly at the WCL, which is within the toe of girth welding, as shown in Fig. 22.

At the girth welding toe in Case A, the peak value of the computed axial stress is 606 MPa. Combined with the computed value of the hoop stress at the same point, equal to 458 MPa, this results in a value of the von Mises stress equal to 547 MPa. This is larger than the yield strength of AISI 10305 welding material at room temperature, which indicates that cracks are likely to have been initiated. In contrast to Case A, the peak value of the computed axial stress is 480 MPa at the girth welding toe in Case B combined with hoop stress at the same point, 200 MPa. In this case, the value of von Mises stress is equal to 417 MPa which is lower than the yield strength of AISI 304 welding material at room temperature.
This maximum hoop stress value is equal to 578 MPa at 2.1 mm in Case A, and combined with the corresponding value of the axial stress, which is equal to 297 MPa, results in a von Mises stress equal to 500 MPa which is larger than the yield stress of stainless steel welding material, which is 438 MPa. This implies that some material failure is predicted by the numerical model, which is in fact confirmed by a scanning electron microscopy (SEM) image as shown in Fig. 23, which showed the presence of some cracks in the material in Case A at the weld toes, because of high stress concentrations [34]. The cracks are particularly existed at the meeting point of weld overlay, girth welding and AISI 10305 pipe. Furthermore, other factors also play a key role to initiate welding cracks such as phase transformations, voids, slag inclusion, undercut and gas pore [35, 36].

From Fig. 22, the maximum computed value of the residual hoop stress in Case B is 470 MPa and combined with the axial stress value, which is equal to 370 MPa, at the WCL, results in a von Mises stress equal to 428 MPa. This is lower than the yield stress of AISI304 welding.
material, which is 438 MPa. The values of residual von Mises stress at the HAZ of weld overlay for both cases are lower than the yield stress of AISI 304 base material, that is 265 MPa. In that case, it is better to use the austenitic stainless steel as a welding material for the girth welding to join two segments of lined pipe together. This reason is attributed to the larger coefficient of thermal expansion which in turn increases its capability to expand and contract naturally during welding. In contrast to that, the AISI 10305 material cools more rapidly and shrinks faster during natural cooling due to its thermal conductivity [21].

Fig. 23 Cracks at weld toes (meeting points of weld overlay, girth welding and outer pipe) in Case A. It is can be noted from Fig. 21 that the numerical residual axial stresses for both cases reduce smoothly up to 77 mm along the axial direction. Afterwards, the numerical residual axial stresses remain steady at c. 120 MPa. In a similar manner, from Fig. 22, one may note that the numerical hoop residual stresses for both cases attain constant values equal to 113 MPa at c. 91 mm on the axial direction.

The experimental results by means of the hole drilling technique, with a reference diameter of 2 mm, are consistent with the numerical values for axial and hoop stresses in both cases. On the inner surface, it can be concluded that stress results in the welding zone and its proximity are not influenced by the pre-heat treatment for both cases. From the point at which the results start being stable and constant for Cases A and B, the TFP process solely affects the residual stresses. Based on Fig. 17, the region located between these two regions has a combined effect of welding and TFP process together. In other words, the residual axial stresses affected by both the TFP and welding processes are located within the region 24 mm <Z< 77 mm from the WCL for both cases. Based on Fig. 18, the residual hoop stresses influenced by the pre-heat treatment and welding process are located in the region 37 mm <Z< 91 mm from the WCL for Cases A and B. The reason for this can be attributed to the higher temperatures at the weld zone and its vicinity which in turn soften the stainless steel material AISI 304 [37]. In this case, the initial residual stresses induced by the pre-heat
treatment are removed completely. The effect of soft temperature gradually reduces starting from a point located at 24 mm to be completely removed at a distance of 91 mm along the axial direction.

5.2. Residual stresses on the outer surface

As it is clear from Fig. 24, the numerical and experimental axial residual stresses on the outer surface for Cases A and B are plotted along the axial direction at the circumferential angle of 180°. Likewise, the numerical residual axial stresses attain uniform values at axial distances of 67 and 77 mm from the WCL for Cases A and B, respectively. Beyond these points, the residual axial stresses reach a constant value which is about zero stress for both cases. The experimental results are in good agreement for each case.

![Fig. 24 Axial residual stresses with respect to the axial direction at 180° (outer surface).](image)

The hoop residual stresses in both cases on the outer surface at 180° central angle is subject to the same arrangement as depicted in Fig. 25. In a similar manner, it is observed that the predicted hoop residual stresses attain uniform values at distances of c. 91 and 73 mm from the WCL for Cases A and B, respectively. These stresses remain constant with equal values slightly over zero stress for both cases. The experimental results are consistent in both cases.
Based on Fig. 19, it can be concluded that the residual axial stresses affected by both the TFP and welding processes are located within the regions $24 \text{ mm} < Z < 77 \text{ mm}$ and $14 \text{ mm} < Z < 67 \text{ mm}$ from the WCL for Cases A and B, respectively. Likewise, based on Fig. 20, the residual hoop stresses affected by both the pre-heat treatment and welding process are located within the regions $37 \text{ mm} < Z < 91 \text{ mm}$ and $20 \text{ mm} < Z < 73 \text{ mm}$ away from the WCL for Cases A and B, respectively. It can be observed that changing the girth welding material to austenitic stainless steel shortens the region affected by the welding process only and lengthens the region influenced by just the initial residual stresses induced by the TFP. This can be attributed to the larger thermal expansion coefficient and lower conductivity of stainless steel.

Attention should be paid to the opposite distribution shapes for the residual stresses on the inner and outer surfaces, and for both the axial and hoop components. This can be related to the different values of the mechanical strains found on both surfaces, as reported earlier in Section 4.2.1. Such a difference can be attributed to the welding sequence of passes, which leads initially to heating of the inner surface more than the outer surface, in the weld overlay and the first girth welding pass, while the outer surface is heated to higher temperatures than the inner surface in the second girth welding pass.

6. Verification and validation

It is important to repeat the experiments under the same conditions in order to validate the experimental results. To this end, residual stress gauges, FRS-2, were used to check the values of stresses for identical two tests. Furthermore, a Bruker D8 Advance X-ray diffractometer instrument (XRD) was also utilised to increase the reliability and certainty. X-ray diffraction was used to measure residual stresses on welded samples mechanically ground.
using 30 μm grit size SiC abrasive papers. Samples were then electropolished in 5% perchloric acid in methanol solution to remove the deformed layer. XRD was equipped with a Cr-Kα1 cathode ray tube directed towards the samples with a diffraction angle (2θ = 156°).

The residual stress gauges, depending on drilling a vertical hole in the middle, were attached on the inner surface for both cases. The residual stress results obtained from the first and second tests at 180° central angle along the axial direction on the inner surface are plotted in Fig. 26. The test results are also compared against the XRD results which show very good correlation between them.

To validate the accuracy of the numerical results, a finer mesh model has been performed for thermal and mechanical analyses. The finer mesh model is composed of 34920 quadratic hexahedral elements with size equal to or smaller than 0.5 time element size used in the normal mesh model, as shown in Fig. 27. For Case A, the numerical thermal history for the finer mesh model, denoted as 0.5h, is compared against that of the normal mesh model, denoted as 1h, at different central angles 90°, 180° and 270° at the WCL as shown in Fig. 28. It can be seen that there is very good correlation in the thermal histories between the finer mesh and normal mesh models. In the mechanical analysis, furthermore, the axial residual stresses of the finer mesh model are also very consistent with their counterparts of the normal mesh at 180° on the inner surface as depicted in Fig. 29. Consequently, the normal mesh deployed in both Cases A and B is as appropriate to obtain numerical results thermally and mechanically with much less computational time.

![Fig. 26 XRD and test residual stress results for two identical tests of (a) Case A and (b) Case B at 180° on the inner surface.](image-url)
Fig. 27 3-D FE model with finer mesh

Fig. 28 The thermal history of second-pass girth welding at 90°, 180°, 270° central angles on the WCL for finer and normal mesh of Case A
7. Conclusions

From the study that has been conducted, it is possible to conclude that the 3D FE thermo-mechanical models have been developed using ABAQUS to explore the effect of girth welding material on lined pipe welding. The numerical thermal and mechanical results have been compared against their experimental counterparts using various methods. A new procedure of pre-heat treatment, so called TFP process, has been performed to insert the austenitic stainless steel liner inside the carbon steel backing pipe. The residual stresses produced by the TFP process are taken into account in the FE models.

This paper provides details of the investigation of the influence of girth welding material on thermal fields and residual stresses. The numerical and experimental results point out that changing the girth welding material to austenitic stainless steel has a significant influence on the residual stresses on the outer surface of a lined pipe. Depending on the predicted numerical and recorded experimental results, the main findings can be addressed as follows:

(1) It is observed that the thermal distributions are in both cases in good agreement. The maximum numerically and experimentally thermal discrepancies between points in Case A and their counterparts in Case B is less than 3%. From the findings of our study, it is possible to point out that the temperature fields during welding are not influenced with the girth welding materials.

(2) During welding, the transient numerical mechanical strains and pure experimental mechanical distributions correlated well for Cases A and B. During natural cooling, the axial strains gradually decrease on the outer surface, AISI 10305 pipe, whereas...
increasing on the inner surface, AISI 304 pipe, to form pure bending at the FZ and HAZ.

(3) The lengths of residual plastic strain zones influenced by the welding process on the outer surface in Case B are narrower than those in Case A along the axial direction. This can be attributed to the larger coefficient of thermal expansion for the austenitic stainless steel. In this case, the stainless steel welding has the capability to expand and contract during welding.

(4) The residual stress curves can be sectioned into three regions based on the influences of the welding and the TFP processes. In general, the lengths of residual hoop stress zones affected by welding temperatures along the axial direction are wider than those of residual axial stress zones because of the effect of boundary conditions and the temperatures.

(5) Replacing the girth welding mild steel by austenitic stainless steel leads to significant reductions in the length of the regions affected by welding temperatures with insignificant initial residual stresses induced by the TFP process on the outer surface. On the inner surface, the residual stresses are not affected by changing the girth welding material beyond the FZ in spite of the presence of remarkable initial residual stresses generated by the TFP process.

(6) To increase the reliability of the results obtained in this study, the welding tests have been replicated twice under the same conditions. Furthermore, X-ray diffraction has been utilized to verify the results measured by residual stress gauges.

References


