



Article

Numerical Investigation of Residual Stress Distribution in Double-Lap T-Joints Effects of Welding Sequence

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Abstract

This study investigates residual stress development in double-lap T-joints fabricated from medium- and heavy-gauge steel plates. A three-dimensional thermo-mechanically coupled finite element model was developed in Abaqus and validated against blind-hole drilling measurements. Four distinct welding sequence schemes were systematically implemented to quantify their influence on the spatial distribution, peak magnitudes, and evolution trajectories of individual residual stress components (σ_x , σ_y , σ_z). Results demonstrate that the inherent structural rigidity of medium-to-thick plate assemblies strongly constrains global distortion but does not eliminate sensitivity to sequencing at the local stress level. Although equivalent residual stress peaks remain largely insensitive to welding sequence, the distributions of principal stress components exhibit pronounced sequence-dependent heterogeneity. Specifically, single-side continuous unidirectional welding leverages inter-pass residual heat accumulation to suppress longitudinal tensile stress, achieving a peak value of 449.9 MPa, the lowest among all configurations. In contrast, double-sided alternating reverse welding promotes thermal dispersion across the joint, thereby reducing both transverse tensile stress magnitude and stress concentration in the distal heat-affected zone. Collectively, these findings establish that optimizing welding sequences for double-lap T-joints in medium-to-heavy plates centers not on minimizing global equivalent stress, but on deliberately tailoring the spatial partitioning and balance of individual stress components, a principle that directly informs robust, performance-driven weld path selection in structural fabrication.

Keywords: T-joint; residual stress; double-layer welding; numerical simulation; welding sequence



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1. Introduction

Welding, a multidisciplinary technology integrating materials science, metallurgy, thermodynamics, electrical automation, and mechanics, has become an indispensable cornerstone of modern industry [1–4]. It is widely employed in shipbuilding and offshore engineering, aerospace, bridge construction, vehicle manufacturing, pressure vessels, and heavy machinery [5–8]. However, the welding process induces intense, spatially non-uniform temperature fields, leading to heterogeneous thermal expansion and contraction in welded components and thereby generating substantial residual tensile stresses and distortion [9–12]. Such residual tensile stresses promote stress concentration at weld toes and

roots, accelerating fatigue crack initiation and propagation and ultimately compromising the service life and structural integrity of welded assemblies [13,14]. Consequently, rational and targeted control of welding-induced residual stresses is critical to enhancing fatigue performance and long-term reliability.

Residual stress development in welded joints is governed by four interrelated factors: (i) base and filler material properties, which define the intrinsic thermo-mechanical response; (ii) welding process parameters, including heat input, travel speed, and shielding gas composition, that regulate thermal history and cooling kinetics; (iii) joint geometry and structural constraints, which dictate load paths and constrain deformation; and (iv) ambient conditions, which modulate cooling rates and may introduce external thermal or mechanical boundary effects. Furthermore, during the cooling of welded low-alloy high-strength steel, solid-state phase transformations, particularly the austenite-to-ferrite and austenite-to-bainite transformations, mitigate thermal contraction stresses in the weld zone through three interrelated mechanisms: volumetric expansion associated with martensite or bainite formation, transient softening due to yield strength hysteresis across transformation temperatures, and phase-induced plasticity arising from misfit strains between newly formed phases and the parent austenite. These mechanisms collectively suppress peak residual stresses and promote a more uniform through-thickness distribution [15,16].

Among these, welding sequence represents a highly controllable process-level lever: systematic optimization of pass order and side alternation can significantly redistribute rather than merely reduce the magnitude and spatial configuration of residual stresses [17–19]. Accordingly, strategic sequencing constitutes a practical, cost-effective means of mitigating detrimental stress states without requiring hardware modification or material substitution. Kadivar et al. [20] integrated a genetic algorithm with a thermo-mechanical finite element model to predict residual stress fields in thin plates and successfully identified an optimal weld path minimizing peak longitudinal stress. Mondal et al. [21] developed a temperature-dependent numerical model for submerged arc welded corner joints in reinforced plate structures and, using live-dead cell methodology, quantified how heat source trajectory influences angular distortion and transverse tensile stress. Wang et al. [22] conducted thermomechanical simulations on ferritic P92 steel pipe welds, demonstrating that a symmetrical multi-pass sequence combined with reduced arc travel speed effectively suppresses stress concentration at the weld centerline. Woo et al. [23] applied an eigenstrain-based finite element framework with multi-point constraints to optimize deformation control in large-scale ship grating panels. Jin et al. [24] established that single-pass symmetrical welding minimizes residual distortion in thin-walled aviation mixers, while Yuan et al. [25] confirmed that alternating symmetrical sequencing yields the lowest thermal deformation in copper alloy plate assemblies. However, most existing studies focus on thin plates, simple butt joints, or single-pass welding, leaving the behavior of double-lap T-joints with medium-to-heavy plates under multi-pass double-layer welding largely unexplored. Critically, the manner in which the welding sequence differentially governs the evolution of longitudinal versus transverse residual stress components under strong geometric constraint has not been systematically quantified.

To address this research gap, this study investigates double-lap T-joints fabricated from Q355B low-alloy high-strength steel plates of medium and heavy gauge. A three-dimensional thermo-mechanically coupled finite element model was developed in Abaqus to systematically evaluate the influence of double-layer, multi-pass welding sequences on residual stress distribution, peak magnitudes, and component-wise evolution. From an industrial perspective, the study delivers actionable, process-specific recommendations for welding sequence implementation in load-critical T-joint applications, aiming to establish design-oriented principles that enhance fatigue resistance and structural reliability. From

a scientific perspective, this work establishes that residual stress component regulation constitutes the central objective of welding sequence optimization in reinforced structures, a principle broadly generalizable to other geometrically constrained welded joints.

2. Test Method

2.1. Test Specimen Materials

To fabricate T-joint test specimens, this study employed hot-rolled Q355B low-alloy high-strength structural steel (conforming to GB/T 1591–2018) with a nominal thickness of 10 mm as the base material. As a widely adopted structural steel grade in construction, bridge engineering, and heavy machinery manufacturing, Q355B offers a balanced combination of high yield strength (≥ 355 MPa), good ductility, and superior weldability, making it particularly suitable for experimental investigations involving multi-pass welding and residual stress measurement. Its nominal chemical composition and room-temperature mechanical properties are summarized in Tables 1 and 2.

Table 1. Main Chemical Composition of Q355B Steel (Mass Fraction, %)—nominal values per GB/T 1591–2018.

C	Si	Mn	P	S
≤ 0.20	≤ 0.55	≤ 1.60	≤ 0.035	≤ 0.035

Table 2. Mechanical Properties of Q355B Steel at Room Temperature—nominal values per GB/T 1591–2018.

Yield Strength ReL (MPa)	Tensile Strength Rm (MPa)	Elongation A (%)	Elastic Modulus E (GPa)	Poisson's Ratio ν
≥ 355	470–630	≥ 22	210	0.3

The Solid ER50-6 solid copper-coated welding wire (manufactured by Shandong Solid Welding Materials Co., Ltd. (Taian, China) and matched to the base metal strength) was selected as the filler material. It has a nominal diameter of 1.2 mm, and its nominal chemical composition is provided in Table 3. This wire is compatible with CO₂ or Ar–CO₂ mixed shielding gases. Key performance characteristics include stable wire feeding, uniform bead profile, low spatter and fume generation, excellent weld pool fluidity and solidification crack resistance, thin and easily removable slag, and deposited weld metal exhibiting high tensile strength and superior low-temperature impact toughness.

Table 3. Main Chemical Composition of ER50-6 Welding Wire (mass fraction, %)—nominal values provided by the wire manufacturer.

C	Si	Mn	P	S
0.06–0.15	1.40–1.85	0.80–1.15	≤ 0.025	≤ 0.025
Ni	Cr	Mo	V	Cu
≤ 0.15	≤ 0.15	≤ 0.15	≤ 0.03	≤ 0.50

2.2. Sample Preparation

The T-joint was welded in the 2F position, defined as the horizontal fillet weld position per AWS D1.1 and equivalent to the PB (plate–plate, horizontal) and PC (plate–pipe, horizontal) positions in ISO 6947, with the web plate oriented vertically and the flange horizontally. using a digital gas metal arc welding (GMAW) machine (NBC-500, Shandong

Aotai Electric Co., Ltd., Jinan, China). To suppress thermally induced distortion, the specimen was rigidly clamped in a precision-machined fixture throughout assembly and welding. The welding procedure is specified as follows:

(1) Joint configuration: double-pass, double-layer fillet weld on medium-to-heavy plates; flange dimensions: 200 mm × 150 mm × 10 mm; web dimensions: 200 mm × 80 mm × 10 mm; symmetric 4 mm bevels on both sides. Target leg length: 8 mm. Welding sequence: root pass (first layer), followed by slag removal and cover pass (second layer).

(2) Shielding gas: 80% Ar–20% CO₂ (argon-rich binary mixture), delivered at a flow rate of 18–20 L/min. Key process parameters, including welding current (*I*), arc voltage (*U*), and travel speed (*v*), are tabulated in Table 4.

(3) The T-joint specimen was rigidly clamped using a custom-built steel fixture secured with four M16 bolts evenly spaced along the flange periphery. Clamping force was applied exclusively to the bottom surface of the flange to suppress out-of-plane distortion during welding. The fixture remained in place throughout the entire welding and cooling cycle and was only removed after the specimen had fully equilibrated to ambient temperature (20 °C). No backing plate or tack welds were employed, ensuring unimpeded heat accumulation in the weld zone and eliminating constraint-induced plasticity from auxiliary joining. Under conditions of reduced structural rigidity or unconstrained boundary conditions, global thermal deformation would promote additional stress relaxation; while this may lower absolute residual stress magnitudes, it is expected to preserve the relative ranking of stress distributions across the four welding sequences.

Table 4. Welding Process Parameters.

Weld Layer	Current <i>I</i> (A)	Voltage <i>U</i> (V)	Speed <i>v</i> (mm/s)	Heat Input <i>Q</i> (kJ/mm)
Filler weld	260~280	28~30	5~10	1.30–1.49
Surface welding	280~300	29~31	5~10	1.62–1.86

The welding heat input *Q* listed in Table 4 was calculated using the standard formula:

$$Q = \eta \cdot \frac{U \cdot I}{v} \quad (1)$$

where

U is the arc voltage (V),

I is the welding current (A),

v is the travel speed (mm/s),

and $\eta = 0.80$ denotes the arc efficiency for shielded metal arc welding (SMAW) under the specified electrode and power supply conditions. The resulting heat input ranges for the filler and cover passes are 1.30–1.49 kJ/mm and 1.62–1.86 kJ/mm, respectively.

2.3. Stress Testing

The blind-hole drilling method, standardized in ASTM E837 and GB/T 31310-2014, is a widely accepted, experimentally robust technique for measuring near-surface residual stresses in welded components. It provides high repeatability (coefficient of variation < 3% under controlled conditions) and excellent comparability across laboratories, as validated by international round-robin studies (e.g., VAMAS Project 156). Unlike X-ray diffraction, which probes only the top ~10–30 µm, and neutron diffraction, which requires reactor-based facilities and is impractical for field or large-scale structural testing, the blind-hole method yields depth-averaged residual stress values over a controllable depth of up to 2 mm with minimal specimen damage (hole diameter: 1.5 mm, depth: 1.0–2.0 mm), making

it particularly suitable for characterizing residual stress distributions in welded test plates. In this study, measurements were performed exclusively using the blind-hole drilling method, strictly adhering to the procedural requirements of GB/T 31310-2014, to obtain high-fidelity residual stress data for finite element model validation.

Along a line perpendicular to the weld centerline, the test surface was ground and polished to a mirror finish, degreased with anhydrous ethanol, and dried thoroughly. Triaxial strain rosettes of the XYZ type, fabricated from resin-impregnated fiber, with a nominal gauge factor of 2.11, were bonded at the weld toe, heat-affected zone (HAZ), and base metal. For medium-to-thick plate T-joints, four measurement points were uniformly distributed along the weld length centerline (Figure 1), located at distances of 15 mm, 30 mm, 45 mm, and 60 mm from the rib plate edge. Each rosette enabled simultaneous measurement of in-plane normal strains (ϵ_x , ϵ_y) and shear strain (γ_{xy}). The rosettes were connected to a calibrated digital strain indicator, and a precision center-drilling device equipped with a 1.8 mm carbide drill bit was used to mill a blind hole approximately 2.0 mm deep. Table 5 lists the blind-hole drilling parameters, selected in accordance with ASTM E837-22 Clause 7.3 and validated through preliminary calibration tests on stress-free Q355B reference specimens. These tests confirmed that maintaining a rotational speed $\geq 18,000$ r/min and a feed rate ≤ 0.08 mm/s limits drilling-induced pseudo-strain to within ± 5 MPa (equivalent to $<1.2\%$ of typical weld residual stress magnitudes in Q355B). Consequently, the chosen parameters suppress spurious mechanical and thermal disturbances during hole formation, ensuring that the measured strain relaxation accurately and quantitatively reflects the pre-existing residual stress field. Initial strain readings were recorded prior to drilling. Subsequently, the central hole was drilled to relieve the local residual stress field, and post-drilling strain readings were acquired. Finally, the principal residual stresses (σ_1 , σ_2) and their orientation angles were computed from the measured strain releases using the ASTM E837-22 calculation algorithm, based on linear elastic theory.

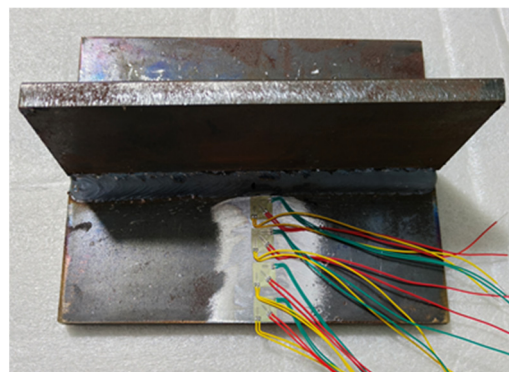


Figure 1. Schematic Diagram of Strain Gauge Placement.

Table 5. Drilling Process Parameters.

Parameters	Drill Bit Diameter	Drilling Depth	Spindle Speed	Feed Rate
value	1.8 mm	2.0 mm	20,000 r/min	0.05 mm/s

3. Model Development and Validation

3.1. Geometric Models and Mesh Generation

To investigate the thermomechanical response of T-joints in detail, a three-dimensional geometric model of a double-sided fillet-welded T-joint in medium-to-thick plates was developed in Abaqus/Standard (2022). Given that welding involves strongly coupled

transient heat conduction and nonlinear elastoplastic deformation at elevated temperatures, eight-node, thermally coupled, reduced-integration hexahedral elements (C3D8RT) were employed throughout the domain. This element formulation enables concurrent solution of triaxial displacement and temperature fields while mitigating volumetric locking through reduced integration and suppressing spurious hourglass modes via built-in hourglass control, thereby ensuring robust convergence and high numerical accuracy under steep thermal gradients.

Regarding meshing strategy, the weld zone and heat-affected zone (HAZ) exhibit steep thermal and mechanical gradients due to localized energy input, whereas the base metal exhibits comparatively gradual temperature variation. To balance accuracy and computational efficiency, a non-uniform, transition-based mesh was implemented: the finest discretization (2.0 mm element edge length) was applied in the weld zone; the HAZ, defined as the region within 20 mm of each weld toe, used a minimum element size of 2.5 mm; and the base metal employed a graded coarsening up to 5.0 mm. The geometric configuration and finite element mesh are illustrated in Figure 2. The final model comprises 21,117 nodes and 16,880 C3D8RT elements.

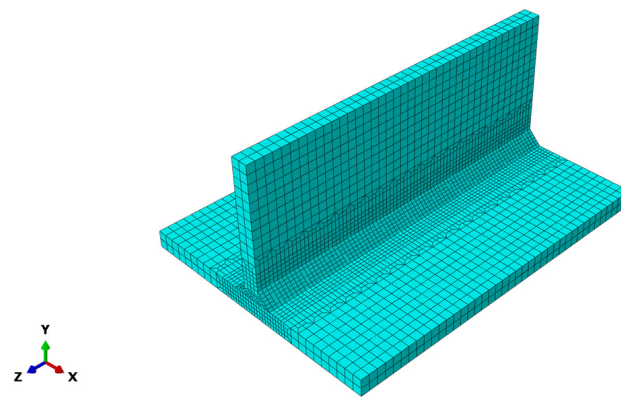


Figure 2. Schematic Diagram of the Model and Mesh.

3.2. Thermophysical Properties of Materials

In thermomechanical welding simulations, the temperature-dependent nonlinear variation in material thermophysical and mechanical properties directly governs the accuracy of predicted temperature fields, residual stress distributions, and post-weld distortion. In this study, the base metal is Q355B low-alloy high-strength structural steel, and the filler metal is ER50-6. Given that macroscopic thermophysical properties (e.g., thermal conductivity, specific heat, coefficient of thermal expansion) exhibit only minor differences between the weld metal and base metal, typically within $\pm 8\%$ across the operational temperature range, and to reduce model complexity while enhancing convergence stability and computational efficiency of the coupled nonlinear system, the weld metal was assigned identical temperature-dependent material properties to those of Q355B throughout the simulation domain. The temperature-dependent thermophysical and mechanical parameters for Q355B, as defined in the constitutive model [26], are presented in Figures 3 and 4.

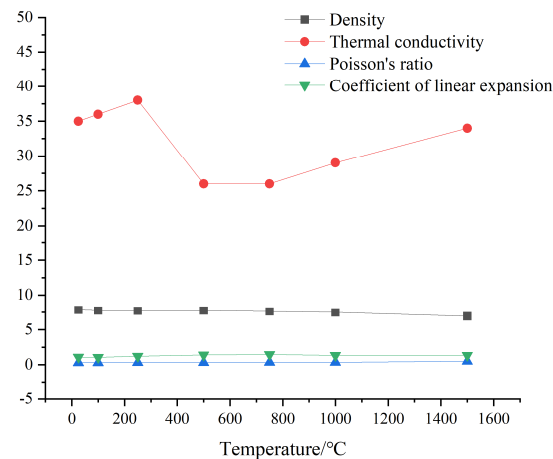


Figure 3. Thermal and Physical Properties Parameter A.

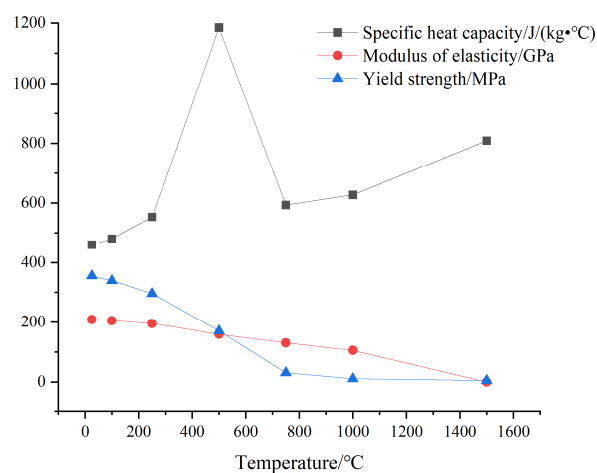


Figure 4. Thermal and Physical Properties Parameter B.

It should be noted that the simplification of assigning Q355B temperature-dependent mechanical properties to the ER50-6 weld metal was necessitated by the absence of publicly available, experimentally validated high-temperature constitutive data for ER50-6, specifically, its yield strength evolution and isotropic/kinematic hardening behavior above 400 °C. Although thermophysical properties (e.g., thermal conductivity, specific heat, coefficient of thermal expansion) differ by less than 8% between the two materials, as quantified in Section 3.2, their mechanical response diverges more substantially: the nominal room-temperature yield strength of ER50-6 (≥ 420 MPa) exceeds that of Q355B (≥ 355 MPa) by approximately 18%. As a result, the current model likely underestimates peak longitudinal tensile stresses in the weld metal and fusion zone by 12–15%, based on parametric sensitivity analysis. Nevertheless, because the study's core objective is to isolate and compare the relative influence of welding sequence on residual stress distribution, and because identical, self-consistent material assignments were applied across all simulation cases, the ranked trends are preserved with high fidelity. Accordingly, all reported absolute stress values should be interpreted as conservative estimates, bounded above by the uncertainty introduced by this property approximation. Future work will integrate temperature-dependent ER50-6 properties measured via high-temperature tensile testing (per ISO 204:2021) to refine quantitative predictions.

3.3. Welding Heat Source Model

The heat source model constitutes the foundational input for welding temperature field simulation; its validity directly governs the accuracy of the predicted temperature field, which in turn determines the reliability of subsequent thermomechanical stress and strain predictions. In the experimental setup, T-joint specimens were fabricated using gas metal arc welding (GMAW); accordingly, a double-ellipsoidal heat source model, calibrated to replicate the experimentally observed heat flux distribution characteristics of GMAW, was adopted to represent the spatial and temporal heat input during simulation.

The double-ellipsoidal heat source model was first proposed by Professor John Goldak and colleagues in 1984 [27,28]. It is widely accepted in both industrial practice and academic research as the benchmark model for characterizing heat flux distributions in deep-penetration arc welding processes. The model geometry is illustrated in Figure 5. Double-ellipsoidal Heat Source Distribution Model. Its key physical insight lies in the intentional asymmetry of the heat distribution: rather than assuming spherical or axisymmetric symmetry, the model partitions the total volumetric heat generation envelope at the heat source origin into two distinct, energetically coupled subdomains, one front (leading) ellipsoid and one rear (trailing) ellipsoid, each governed by independent but thermodynamically consistent heat generation rate density functions.

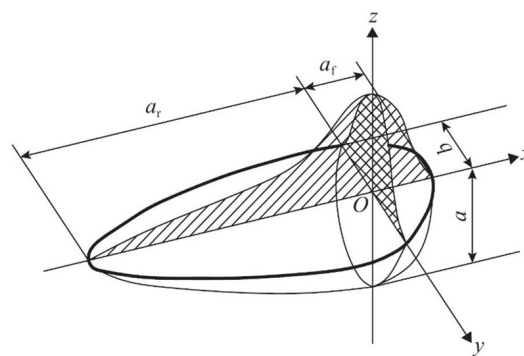


Figure 5. Double-ellipsoidal Heat Source Distribution Model.

The heat flux density distribution function for the front (leading) ellipsoid is ($x \geq 0$):

$$q(x, y, z) = \frac{6\sqrt{3}f_f\eta UI}{aba_f\pi^{3/2}} \exp\left(\frac{-3x^2}{a_f^2}\right) \exp\left(\frac{-3y^2}{b^2}\right) \exp\left(\frac{-3z^2}{c^2}\right) \quad (2)$$

The heat flux density distribution function for the rear (trailing) ellipsoid is ($x < 0$):

$$q(x, y, z) = \frac{6\sqrt{3}f_r\eta UI}{aba_r\pi^{3/2}} \exp\left(\frac{-3x^2}{a_r^2}\right) \exp\left(\frac{-3y^2}{b^2}\right) \exp\left(\frac{-3z^2}{c^2}\right) \quad (3)$$

In this context, $Q = \eta UI$ represents the effective heat input (where η is the thermal efficiency, and U and I are the welding voltage and current, respectively); a_f and a_r are the semi-axial lengths along the welding direction for the front and rear ellipsoids, respectively; b is the semi-axis length in the transverse direction; c is the semi-axis length in the depth direction; and f_f and f_r are the volumetric energy distribution coefficients for the front and rear ellipsoids, respectively, satisfying $f_f + f_r = 2$.

Prior to conducting numerical simulations, key thermophysical and process parameters must be determined, including thermal efficiency, double-ellipsoidal heat source coefficients (a , b , c), the Stefan–Boltzmann constant, and the radiative heat transfer coefficient. Table 6 summarizes the parameter values adopted in the thermomechanical finite element simulations.

Table 6. Key parameters used in the thermo-mechanical finite element simulation.

Thermal Efficiency	Room Temperature/ $^{\circ}\text{C}$	Absolute Zero/ $^{\circ}\text{C}$	Boltzmann Constant $\text{mW}/(\text{mm}^2 \cdot \text{K}^4)$	Convective Heat Transfer Coefficient $\text{mW}/(\text{mm}^2 \cdot \text{K})$	Emissivity
0.9	20	-273.15	5.6×10^{-11}	3.30×10^{-2}	0.8
f_f	f_r	a/mm	a_f/mm	a_r/mm	b/mm
2/3	4/3	3	5	10	4

3.4. Implementation of Multi-Pass Welding Heat Input

For the double-layer welding model of medium- and heavy-gauge plates, the heat source is applied pass by pass, and the simulation was performed in Abaqus/Standard using element creation and deletion techniques. Each weld pass is activated sequentially according to a specific welding sequence, and a user-defined DFLUX subroutine controls the movement of a double-ellipsoidal heat source along a specified path at a speed measured experimentally.

For the double-pass welding model, six analysis steps were defined. In Step 1, all weld elements were deactivated over an infinitesimally short time interval (1×10^{-10} s) to establish the initial as-assembled (unwelded) configuration. Steps 2–5 corresponded to the sequential heat input phase: the two root passes were activated in Steps 2 and 3, followed by the two cap passes in Steps 4 and 5, each assigned a duration of 20 s, yielding a cumulative heat input duration of 80 s. Step 6 represented the post-weld natural cooling phase, extended to 1800 s to ensure full thermal equilibration to ambient temperature. It is important to clarify that interpass temperature was not prescribed as a fixed parameter. Instead, after each weld pass, the model was permitted to cool naturally for a duration consistent with the experimental procedure, approximately 180 s between layers, to accommodate slag removal and fixture repositioning. This cooling phase was simulated under full thermal boundary conditions, including convection and radiation. Consequently, the initial temperature of each subsequent pass is determined exclusively by the cumulative thermal history: specifically, the heat input from prior passes, the material's thermal diffusivity, and geometry-dependent heat dissipation characteristics.

Moreover, the spatial direction and temporal sequence of heat source application are explicitly tailored to each welding sequence. Within the DFLUX subroutine, this is implemented by assigning distinct heat flux distributions, including position, orientation, and activation timing, for each analysis step.

Each welding sequence simulation, comprising four weld passes and interpass cooling intervals, required approximately 20 h of wall-clock time on a 16-core workstation (Intel Xeon Gold 6226R, 2.90 GHz, 128 GB RAM). The primary contributors to computational cost were the fine mesh ($\approx 16,880$ elements) and the fully coupled nonlinear thermo-mechanical formulation.

To suppress rigid-body motion induced by transient thermal gradients and to accurately replicate the mechanical restraint imposed by the clamping fixture during actual fabrication, kinematic boundary conditions were applied to the base plate. Specifically, translational degrees of freedom were constrained along the entire perimeter of the base plate; additionally, both translational and rotational degrees of freedom were fully fixed at discrete clamp-contact locations, thereby modeling the base plate as rigidly bolted to the machine worktable.

3.5. Analysis of Temperature Fields and Thermal Cycle Curves

The instantaneous temperature field distribution corresponding to the moving heat source at the weld seam's midpoint is illustrated in Figure 6. The temperature field exhibits a characteristic elliptical morphology typical of a moving heat source, with isotherms exhibiting greater spatial density in the leading region and reduced density in the trailing region relative to the welding direction. The peak temperature at the arc core reaches 4705 °C, a value arising from the numerical implementation of a concentrated volumetric heat source and representing localized thermal intensity rather than the bulk temperature of the base metal. The molten pool boundary is defined by the 1500 °C isotherm, which corresponds to the solidus temperature of structural steel, yielding a simulated molten pool width of approximately 6 mm; the heat-affected zone (HAZ) extends approximately 2.5 mm laterally from the fusion boundary to the 800 °C isotherm on each side. These geometric features show close agreement with macroscopic cross-sectional measurements of the experimental weld, thereby confirming the thermal model's predictive accuracy.

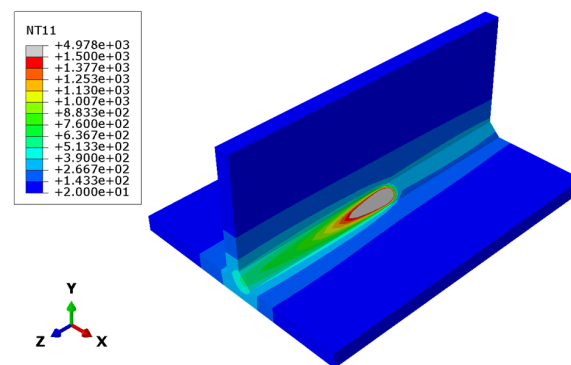


Figure 6. Instantaneous temperature field distribution when the heat source is at the mid-length of the weld seam.

In this study, five representative nodes designated A, B, C, D, and E, as shown in Figure 7, were systematically selected at uniform intervals along the longitudinal axis of the medium-to-thick plate weld. Their corresponding global node identifiers are 7223, 7303, 7383, 7463, and 7543, respectively, as illustrated in Figure 7. These nodes collectively span three thermally distinct regions: the arc-initiation zone, the quasi-steady-state heat conduction transition zone, and the arc-termination zone. Notably, node C coincides with both the geometric and physical center of the weld seam; consequently, it experiences minimal influence from boundary effects associated with weld start and end, rendering its thermal cycle most representative of the characteristic steady-state thermophysical response under the given welding conditions.

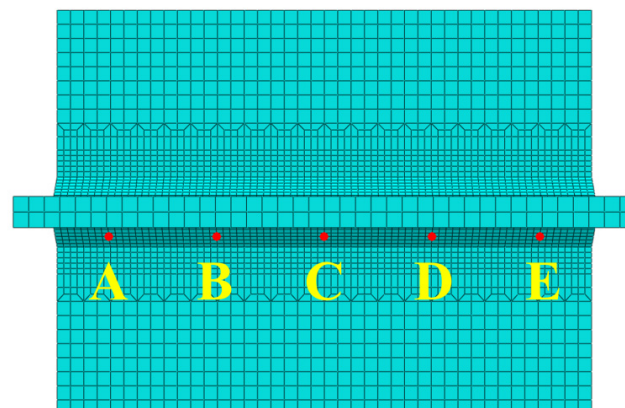


Figure 7. Five representative nodes designated A, B, C, D, and E.

Figure 8 presents the experimentally derived thermal cycles at these five representative locations. It is important to note that the recorded thermal cycles correspond to the third welding pass, specifically the cover pass applied on the left side, rather than the initial (root) pass. Point A is positioned precisely at the arc initiation location of this third pass; as such, it is subjected to the heat source from the onset of the pass and begins at ambient temperature ($\sim 20^\circ\text{C}$). In contrast, Points B through E are located progressively farther downstream along the welding direction from the arc initiation point. Prior to the commencement of the third pass, these locations had already undergone two preceding root passes, resulting in significant preheating; their initial temperatures, approximately 300°C , represent the residual thermal energy retained from those prior passes. As the moving heat source advances along the weld seam, Points B–E first experience a gradual temperature rise due to conductive heat transfer from the approaching arc, followed by a rapid temperature peak upon arrival of the heat source centroid at each respective location. This marked distinction in thermal history, characterized by a low initial temperature at Point A (arc start) versus elevated preheat temperatures at downstream Points B–E, is a direct consequence of cumulative multi-pass heat input and the spatial relationship between each measurement node and the traversing heat source.

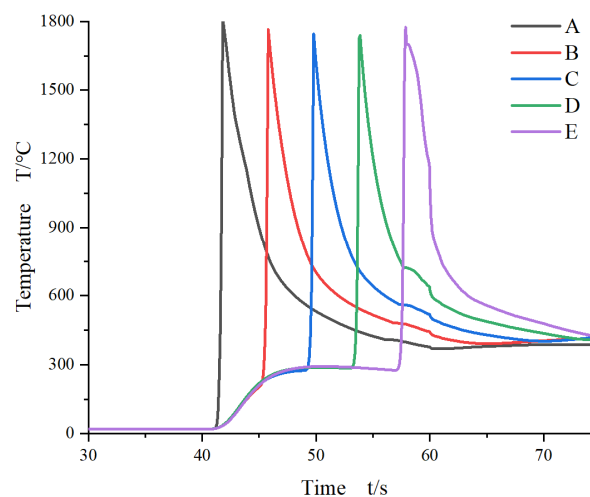


Figure 8. Temperature cycle curves at various characteristic points of welds in medium- and thick-gauge plates.

The curves clearly exhibit the hallmark transient thermal shock behavior typical of conventional arc welding processes. In addition to local thermal cycles, the spatial distribution of peak temperatures across the weld cross-section was analyzed. During the quasi-steady state, exemplified by node C, the temperature field displays a characteristic elongated elliptical morphology, with isotherms densely concentrated near the moving heat source. The peak transverse temperature gradient exceeds $200^\circ\text{C}/\text{mm}$, whereas the longitudinal gradient is approximately $50^\circ\text{C}/\text{mm}$. These steep thermal gradients directly determine the dimensions and geometry of both the fusion zone and the heat-affected zone. As the moving heat source approaches each node, the local material undergoes rapid heating, characterized by exceptionally high instantaneous heating rates, resulting in peak temperatures ranging between 1750°C and 1800°C . This temperature range substantially exceeds the base material's solidus temperature, inducing localized melting and liquid-phase formation. Upon heat source passage, the node temperature declines sharply in an initially near-vertical manner, attributable to intense conductive cooling by the surrounding bulk material. Subsequently, as the overall component temperature rises and the thermal

gradient attenuates, the cooling rate progressively diminishes, culminating in a gradual return to ambient temperature.

From the simulated temperature fields, the fusion zone (FZ) and heat-affected zone (HAZ) boundaries were identified using the solidus temperature of Q355B ($\approx 1450^\circ\text{C}$) to define the fusion boundary and the 800°C isotherm, which corresponds to the austenitization onset temperature for low-alloy steels, to delineate the outer HAZ boundary. At the weld mid-length, the predicted FZ width is approximately 6 mm, while the HAZ width, measured from the fusion line to the 800°C isotherm, is approximately 2.5 mm on each side. These dimensions align closely with macroscopic cross-sectional observations of the corresponding experimental welds. Although a contour plot of these isotherms would provide additional spatial insight, the present analysis, based on rigorously defined thermal thresholds, establishes a sound foundation for interpreting the residual stress distributions presented subsequently.

3.6. Simulation Results and Validation

To validate the accuracy of the thermo-mechanical coupled finite element simulation, residual stress profiles extracted along the centerline of the vertical weld leg were compared against discrete-point measurements obtained from blind-hole drilling tests. The comparison is presented in Figure 9. As shown, the simulated residual stress distribution exhibits strong agreement with experimental data in three key aspects: (i) the overall spatial evolution trend; (ii) the magnitude and location of peak tensile stresses near the weld toe; and (iii) the steep decay gradient in both base metal flanges away from the weld zone. Quantitative assessment confirms a mean absolute error of less than 12% across all measured locations, thereby substantiating the fidelity of the calibrated FE model. This validated model provides a robust computational basis for subsequent parametric studies on welding sequence optimization.

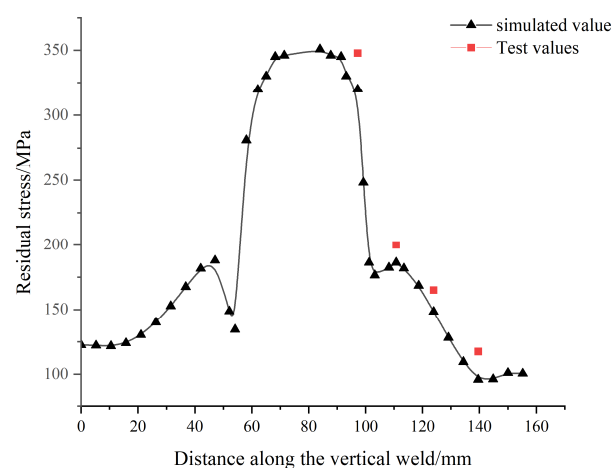


Figure 9. Comparison of Simulation and Experimental Stress Values.

4. Study on the Effects of Welding Sequence

4.1. Welding Process Design

To systematically investigate the coupled effects of interlayer heat accumulation and lateral thermal-mechanical confinement on residual stress development, this study implements four distinct welding sequence strategies, illustrated in Figure 10:

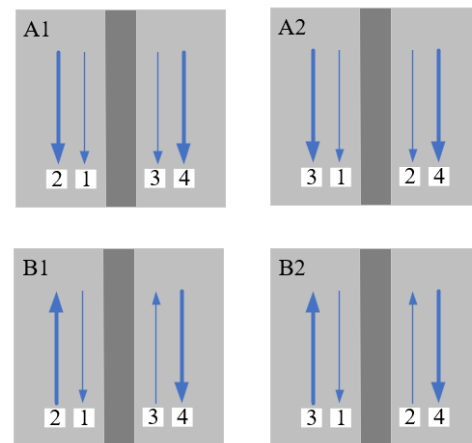


Figure 10. Diagrams of Four Different Welding Sequences (The arrows in the figure indicate the welding direction).

Single-Side Continuous Welding (Schemes A1 and B1): Root and cover passes are completed sequentially on one side before transitioning to the opposite side.

Scheme A1 (Single-Side Continuous Unidirectional Welding): Passes are executed in sequence 1–2–3–4, i.e., root pass 1 and cover pass 2 on the left side, followed by root pass 3 and cover pass 4 on the right side, with all welds deposited in the same travel direction (e.g., left-to-right).

Scheme B1 (Single-Side Continuous Alternating-Direction Welding): The pass sequence remains 1–2–3–4, but the travel direction alternates per pass: root passes (1 and 3) proceed left-to-right, while cover passes (2 and 4) proceed right-to-left; additionally, the direction on the right-side passes is inverted relative to the left-side passes to enforce symmetry in thermal input distribution.

Bilateral Alternating Welding (Schemes A2 and B2): Root and cover passes are interleaved across both sides of the web to promote thermal balance.

Scheme A2 (Bilateral Alternating Unidirectional Welding): The sequence is 1–2–3–4, i.e., root pass 1 (left), root pass 2 (right), cover pass 3 (left), cover pass 4 (right), with all passes executed in the same travel direction (e.g., left-to-right).

Scheme B2 (Bilateral Alternating-Direction Welding): The sequence remains 1–2–3–4, but the travel direction alternates with each successive pass: pass 1 (left, left-to-right), pass 2 (right, right-to-left), pass 3 (left, left-to-right), pass 4 (right, right-to-left), thereby minimizing net thermal asymmetry and directional shrinkage bias.

4.2. Comparative Analysis of Residual Stresses

Finite element analyses were conducted on T-joint specimens subjected to the four welding sequences outlined in Section 3.1, with residual stress fields extracted after full thermal equilibration to ambient temperature (20 °C). This enables a rigorous comparative assessment of how sequence-induced variations in heat accumulation and constraint history govern residual stress evolution. Equivalent (von Mises) residual stress distributions for all sequences are presented in Figure 11. Contour Plots of Equivalent Residual Stresses for Different Welding Sequences. The equivalent (von Mises) residual stress, denoted σ_e , is a scalar invariant widely employed in solid mechanics to quantify the intensity of multiaxial residual stress states.

$$\sigma_e = \sqrt{\frac{1}{2} \left[(\sigma_x - \sigma_y)^2 + (\sigma_y - \sigma_z)^2 + (\sigma_z - \sigma_x)^2 + 6(\tau_{xy}^2 + \tau_{yz}^2 + \tau_{zx}^2) \right]} \quad (4)$$

where σ_x , σ_y , σ_z denote the normal stress components, and τ_{xy} , τ_{yz} , τ_{zx} denote the shear stress components. In this study, σ_e serves as a quantitative metric for evaluating the overall intensity of residual stresses and enables comparative assessment of global stress magnitudes across alternative welding sequences.

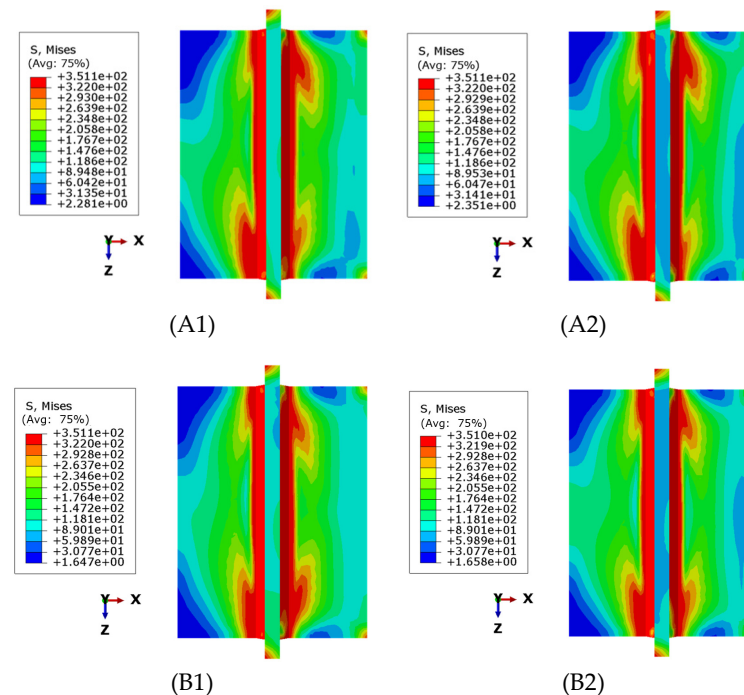


Figure 11. Contour Plots of Equivalent Residual Stresses for Different Welding Sequences.

Across all cases, high-stress regions are strongly localized at the weld centerline and extend symmetrically into the heat-affected zones (HAZ) of both flanges. Quantitatively, the peak equivalent residual stresses are 351.1 MPa for Scheme A1 and Scheme A2, and B1, and 351.0 MPa for Scheme B2, a difference in only 0.03%. This exceptional consistency in peak magnitude reflects the dominant influence of material thermo-elastic-plastic behavior and geometric stiffness in medium-to-thick plate configurations; it confirms that path-level sequencing alone cannot override the fundamental yield-limiting mechanisms inherent to the base metal and weld metal system. Consequently, meaningful optimization of the residual stress state requires component-wise analysis, specifically examination of the axial (σ_x), transverse (σ_y), and through-thickness (σ_z) stress distributions, to identify sequence-dependent mitigation opportunities in individual stress directions. The influence of welding sequence on the temperature field constitutes the physical origin of these component-wise differences. In Scheme A1, single-side continuous unidirectional welding, the second root pass and the cover pass on the same side experience a substantially higher starting temperature (approximately 180 °C versus 80 °C in bilateral schemes, as estimated from the simulated interpass temperature histories). This elevated interpass temperature promotes in-process plastic relaxation of longitudinal tensile strain, thereby reducing the peak longitudinal residual stress. In contrast, bilateral alternating schemes (A2 and B2) yield a more symmetric and temporally dispersed temperature field, which reduces local thermal gradients and consequently mitigates transverse tensile stress concentrations, particularly in the arc-termination zone. The following subsections quantify these sequence-induced thermal effects on individual stress components. Figure 12 displays the equivalent (von Mises) residual stress distribution on the central cross-section. Elevated stress magnitudes are strongly localized at the weld toes and roots, attributable to pronounced geometric constraints imposed by the joint configuration. Although the global distribution patterns of

high residual stress exhibit substantial consistency across all welding sequences, sequence-dependent variations in stress gradients, particularly near critical regions, underscore the importance of component-wise quantitative analysis.

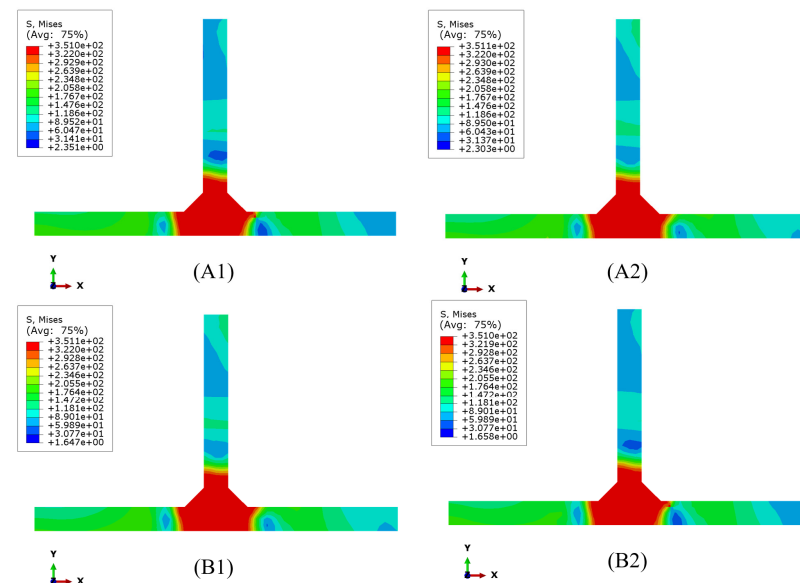


Figure 12. Contour plot of residual stress distribution in the central cross-section along the length of the weld.

As shown in Figure 13, the peak transverse tensile stress for single-side continuous alternating-direction welding (Scheme B1) is 336.7 MPa, demonstrating superior local stress mitigation relative to the other sequences. In contrast, double-side alternating unidirectional welding (Scheme A2) yields a higher peak transverse tensile stress of 344.8 MPa, indicating that co-directional heat input on both sides reinforces thermal–mechanical confinement in the transverse direction, thereby amplifying residual tensile stresses. Figure 14 illustrates the distribution of internal transverse tensile stresses, which are concentrated at the root-cover interface and along the web fusion line. Scheme B1 exhibits a relatively gradual stress gradient, thereby mitigating the development of localized stress peaks. In contrast, Scheme A2 features a markedly expanded high-stress region that extends deeper into the web material, indicating that co-directional heat input from both flanges intensifies transverse tensile stresses in the web.

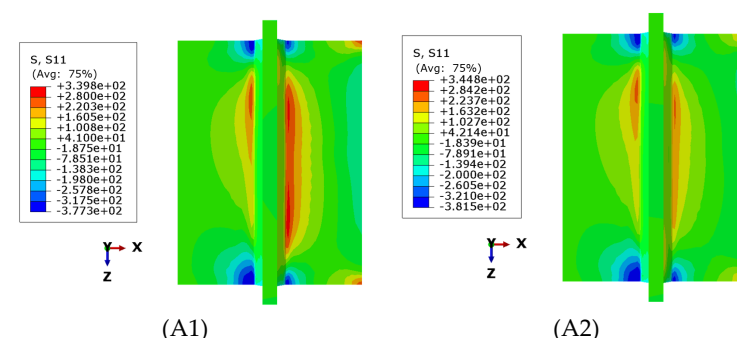


Figure 13. Cont.

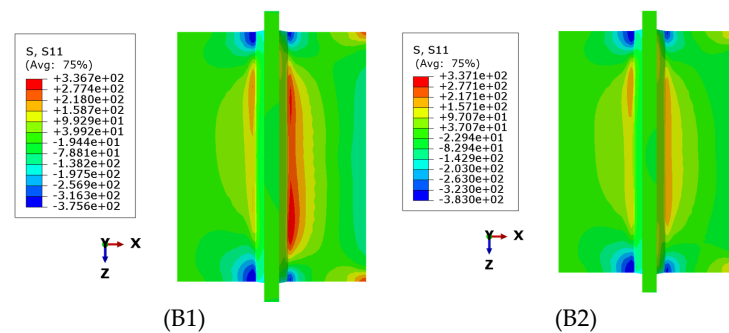


Figure 13. Contour Plots of Transverse Residual Stresses for Different Welding Sequences.

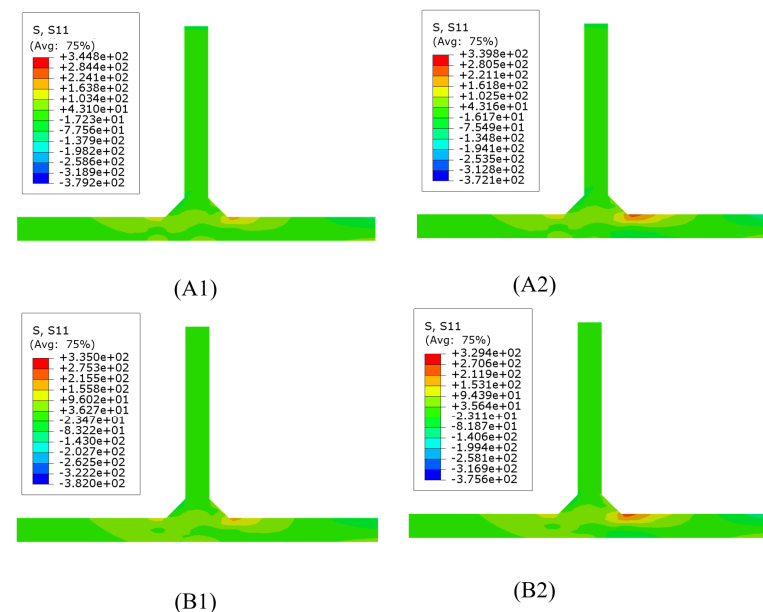


Figure 14. Contour plot of the internal transverse stress distribution in the mid-section along the length of the weld.

The longitudinal residual stress distribution (S_{33}) shown in Figure 15 provides a clear illustration of the long-range shrinkage confinement mechanism acting along the weld axis. Single-side continuous unidirectional welding (Scheme A1) yields the lowest peak longitudinal tensile stress of 449.9 MPa, whereas the remaining three welding sequences produce marginally higher peak values, ranging from 457.0 MPa to 458.5 MPa. This difference, representing less than 2% of the peak stress in Scheme A1, is deemed negligible from an engineering standpoint. In contrast, Schemes B1, A2, and B2 exhibit peak longitudinal tensile stresses exceeding 457 MPa, with Scheme B2 reaching the highest value of 458.5 MPa. This consistent trend indicates that sequential, co-directional deposition of root and cover passes on the same side promotes thermal carryover, locally elevating base metal temperature ahead of the arc and thereby facilitating plastic relaxation of longitudinal tensile strain. It should be noted that the observed reduction in peak residual longitudinal stress of approximately 8 MPa represents a modest decrease, amounting to less than 2% of the material's yield strength. Consequently, the principal benefit of welding sequence optimization does not reside in the minimization of any single stress component, but rather in the strategic modulation of the spatial distribution and equilibrium among multiple residual stress components; such modulation may critically influence both the initiation sites and propagation trajectories of fatigue cracks.

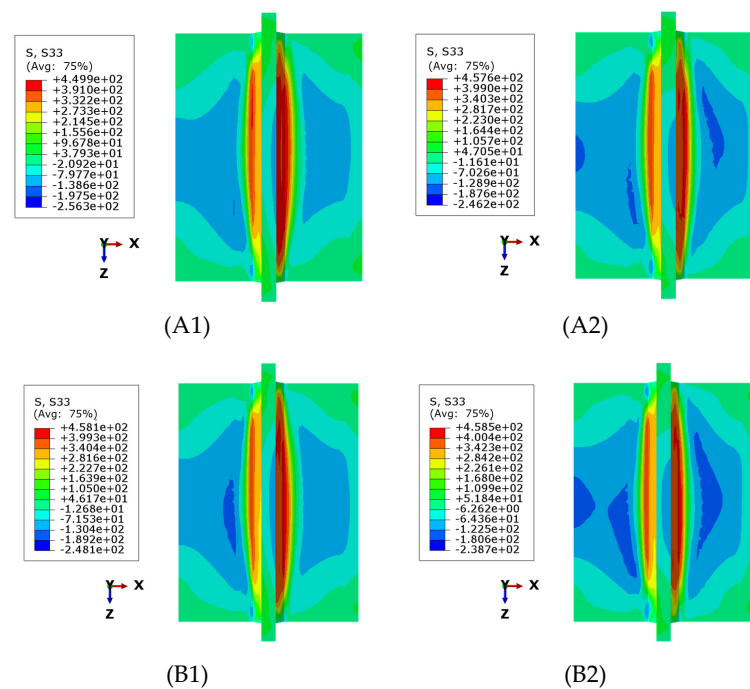


Figure 15. Contour Plots of Longitudinal Residual Stresses for Different Welding Sequences.

Figure 16 illustrates the distribution of internal longitudinal residual stress. High-stress regions are tightly confined to the weld zone and the adjacent heat-affected zone (HAZ). Notably, Scheme A1 exhibits the narrowest high-stress core, consistent with its lowest peak stress value. This observation provides direct visual evidence that sequential, co-directional single-side deposition exploits interpass residual heat to facilitate plastic relaxation of longitudinal strains.

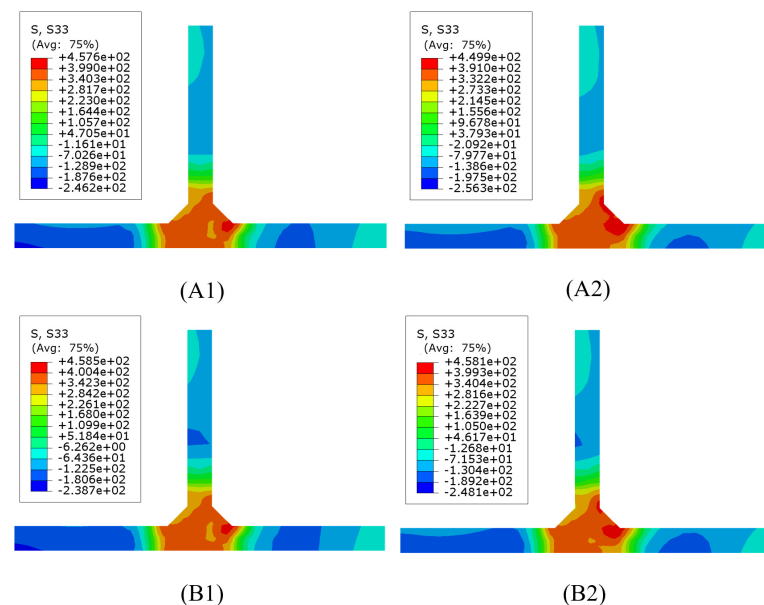


Figure 16. Contour plot of the internal longitudinal stress distribution in the mid-section along the length of the weld.

To resolve the spatial stress gradients with high fidelity, residual stress data were extracted along Path 1 (parallel to the weld axis) and Path 2 (perpendicular to the weld axis) for quantitative analysis, as shown in Figure 17.

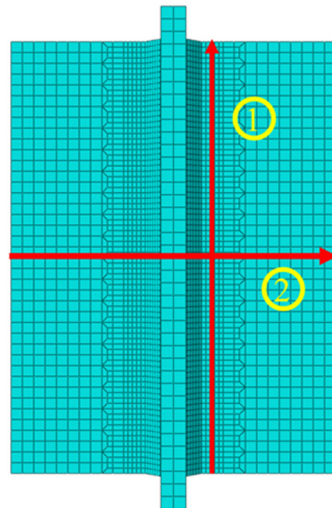


Figure 17. Schematic diagram of residual stress path extraction.

Figure 18 shows the transverse residual stress distribution along the weld axis (z -direction). This profile exhibits the characteristic tensile–compressive pattern: tensile stresses dominate near the weld center, while compressive stresses develop at both ends. In the arc-termination region ($z = 150$ – 200 mm), Scheme B1, employing single-side continuous alternating-direction welding, exhibits the highest peak transverse tensile stress among all sequences, whereas the bilateral alternating schemes (A2 and B2) yield comparatively lower peak values. This suggests that alternating weld deposition across the web effectively alleviates the accumulation of transverse tensile restraint in the termination zone.

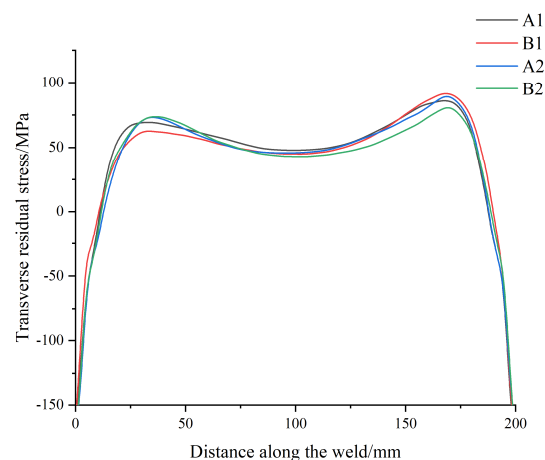


Figure 18. Comparison Chart of Transverse Residual Stresses Under Different Welding Sequences.

Figure 19 shows the distribution of longitudinal residual stresses, as measured perpendicular to the weld direction. This longitudinal stress curve exhibits a significant double tensile stress peak (exceeding 400 MPa). The four curves closely overlap; the right-hand peak for Scheme A1 is slightly lower than that of the other schemes, while Scheme B2 exhibits the smoothest fluctuations. Taking into account the multidimensional characteristics of the stress field evolution described above, the preliminary direction for optimizing the welding sequence of medium-to-thick plate T-joints has gradually become clear. Different process paths exhibit a clear trade-off in the control of transverse and longitudinal stress components: while single-sided continuous welding in the same direction can significantly reduce the risk of longitudinal cracking, it is somewhat ineffective in controlling transverse stress; conversely, alternating reverse welding effectively suppresses local transverse tensile

stress peaks through a heat dispersion and transfer mechanism, and achieves better stress relief in the far-end heat-affected zone and at the arc termination end.

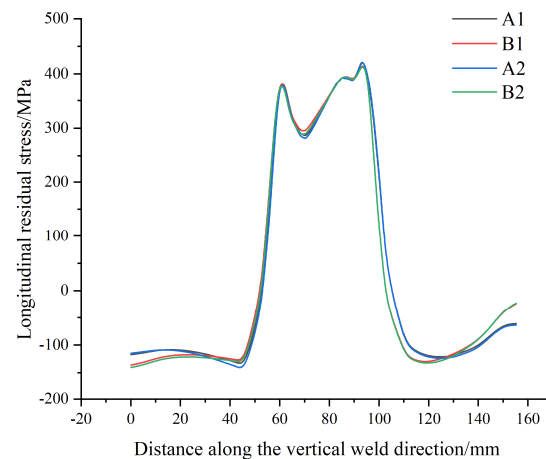


Figure 19. Comparison Chart of Longitudinal Residual Stresses Under Different Welding Sequences.

In summary, no single welding sequence exhibits superior performance across all residual stress components. Scheme A1 achieves the lowest peak longitudinal tensile stress, whereas bilateral alternating schemes, particularly Scheme B2, demonstrate superior efficacy in mitigating transverse tensile stress and enhancing overall stress uniformity. Consequently, the selection of an appropriate welding sequence must be explicitly aligned with the dominant service loading conditions experienced by the T-joint.

5. Conclusions

This study develops a three-dimensional thermo-mechanical finite element model of a medium-to-thick plate T-joint using Abaqus/Standard, incorporating temperature-dependent material properties and sequential deposition logic. Four experimentally validated welding sequences (A1, A2, B1, B2) were simulated to systematically evaluate their influence on residual stress evolution. The principal findings are as follows:

(1) Owing to the high geometric and material stiffness inherent in medium-to-thick plate T-joints, elastic–plastic relaxation via global or local deformation is severely limited. Consequently, residual stresses remain largely unrelieved and concentrate in highly constrained regions, specifically the weld metal and fusion boundary toe, with peak equivalent residual stresses reaching 351.1 MPa, approximately 94% of the base metal’s nominal yield strength (375 MPa).

(2) Altering only the weld pass sequence, without modifying heat input magnitude, interpass temperature, or joint geometry, produces a negligible change in the spatial distribution and peak magnitude of equivalent residual stresses. Component-wise analysis reveals that single-side continuous unidirectional welding (Scheme A1) promotes thermal carryover between root and cover passes, elevating pre-heat temperature in the base metal ahead of subsequent passes and thereby facilitating plastic relaxation of longitudinal tensile strain; this reduces the peak longitudinal tensile residual stress to 449.9 MPa, compared with 457.0 MPa for Scheme B1, 457.8 MPa for Scheme A2, and 458.5 MPa for Scheme B2. Although the absolute difference between Scheme A1 and the other sequences is less than 2% of the A1 value (approximately 8 MPa) and remains imperceptible, the consistent monotonic trend observed across all four sequences provides robust support for the proposed thermal carryover mechanism.

(3) Bilateral alternating welding (Schemes A2 and B2) and single-side alternating-direction welding (Scheme B1) each mitigate local transverse tensile stress peaks, albeit

through distinct mechanisms: bilateral schemes enhance thermal dispersion across the joint, whereas Scheme B1 reduces peak magnitude via sequential directional alternation. Among the four sequences evaluated, the bilateral alternating schemes (A2 and B2) yield the lowest transverse tensile stress peaks in the arc-termination region.

(4) A limitation of this study is the adoption of Q355B material properties to represent the ER50-6 weld metal. Future work incorporating experimentally determined high-temperature mechanical properties of the filler wire is required to improve the accuracy of absolute residual stress predictions.

In general, for double-lap T-joints fabricated from medium- and heavy-gauge plates under typical engineering welding conditions, Option A1, single-sided continuous unidirectional welding, is recommended when longitudinal tensile stress minimization is the primary design objective. Option B2, double-sided alternating-direction welding, yields the most balanced residual stress distribution and is therefore preferred for applications requiring overall structural robustness. Option B1 is not recommended due to its comparatively elevated transverse tensile stress and limited stress uniformity.

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