

Article

Full-Process Multiphysics Simulation and Experimental Study on the Fatigue Performance Enhancement of Butt-Welded Joints of QSTE700TM Through Ultrasonic Impact Treatment

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Abstract

Ultrasonic Impact Treatment (UIT), a prevalent surface-strengthening technology for welded structures, combines mechanical shock and ultrasonic vibration to induce plastic deformation and beneficial residual compressive stress at weld toes, effectively enhancing welded joint fatigue performance. This study adopts a full-process numerical simulation approach, integrating the finite element software ABAQUS and FE-SAFE fatigue-life prediction platform to investigate QSTE700TM high-strength automotive steel butt joints. Considering welding-induced initial residual stress, ABAQUS simulates the welding and subsequent UIT processes; explicit dynamic analysis reveals residual stress evolution, with pre- and post-UIT stress-distribution comparisons. The post-UIT residual stress field is input into a static tensile model to obtain load-stress distributions, which are then imported into FE-SAFE with S-N curves for fatigue-life prediction. Simulation results align well with experimental data: UIT improves the fatigue limit of welded specimens by 31.3% and unwelded ones by 42.9%. Additionally, optical and scanning electron microscopes observe fatigue fracture morphologies to further clarify UIT's fatigue-enhancement mechanism.

Keywords: ultrasonic impact treatment; welding; residual stress; full-process simulation; fatigue life

1. Introduction

QSTE700TM low alloy automotive steel as the key structural material of the modern automotive industry. With its excellent high-strength characteristics, good toughness and suitable formability, it is widely used in the manufacturing of key load-bearing components such as the car body frame and chassis system. With the continuous improvement of the automotive industry's requirements for lightweight design, safety performance and fuel economy, higher standards have been put forward for the strength grade and comprehensive service performance of low-alloy automotive steel [1]. In the automotive manufacturing process, welding is the core technical means to realize the connection of components [2]. However, in the welding process of low-alloy steel, the uneven temperature

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field caused by local high-temperature heat sources causes uneven shrinkage of the material, which inevitably forms a self-balanced residual stress system in the welding joint area [3]. The tensile residual stress at the welded joint can easily become the stress concentration source of crack initiation. It can significantly reduce the fatigue life of materials and pose a potential threat to the integrity and durability of welded structures. In view of this, a new post-weld strengthening technology—ultrasonic impact treatment (UIT)—has been introduced into the field of welded-joint performance optimization [4,5]. Through the synergistic effect of high-frequency mechanical impact and ultrasonic vibration, the technology can locally strengthen the weld-toe area of the welded joint.

Research shows that, as a widely used surface-strengthening technology, the core role of a unit is to introduce the compressive-stress state in the weld-toe area [6,7]. It can effectively reduce or eliminate the harmful residual tensile stress on the joint surface and reduce the stress concentration at the weld toe [8,9], so as to significantly improve the service life, safety and reliability of welded structures, which is an important means to improve the fatigue resistance and stress corrosion resistance of high strength steel welded joints [10–13]. Lago et al. [14] conducted ultrasonic impact on the welded joint made of strenx 700 MC high-strength low-alloy steel, which transformed the near-surface tensile residual stress in the weld metal and heat-affected zone into a compressive residual stress field, with the maximum value from -400 MPa to -800 MPa. The fatigue limit was increased from 370 MPa to 410 MPa. He et al. [15] conducted UIT on the surface of normalized LZ50 steel. The hardened layer with a thickness of $130\text{ }\mu\text{m}$ was obtained, and the surface hardness was increased by 46%. At the same time, the residual compressive stress of the sample reached 601 MPa, and the surface roughness was significantly reduced to $0.21\text{ }\mu\text{m}$. These results are beneficial to improve the fatigue properties of steel. Galtier et al. [16] introduced the research results of various post-weld fatigue-improvement technologies on the fatigue limit and life of welded joints of medium- and high-strength steel. The research shows that UIT is the most effective fatigue-improvement technology, and it also makes it possible for high-strength steel to exert its advantages in the manufacturing of welded structures under variable loads. In conclusion, UIT is the most effective method to improve the fatigue limit and life of high-strength-steel welded joints.

Revealing the evolution law of residual stress before and after UIT is the core scientific problem to clarify the mechanism of UIT technology to improve the fatigue performance of welded structures. At present, relevant studies mainly rely on experimental testing methods (such as blind hole method, X-ray diffraction method, etc.) [17–19]. However, limited by the objective conditions such as high experimental cost and long period, some scholars try to use quasi-static [20,21] or dynamic [22,23] finite element analysis methods to simulate the residual stress field after UIT. As a result, the existing studies generally ignore the initial residual stress field introduced by the UIT process, the geometric discontinuity of welded joints and the dynamic effects in these processes. As a result, it is difficult for the simulated distribution of internal residual stress and the geometric characteristics of the weld toe to coincide with the experimental results at the same time, which affects the accuracy of fatigue-performance prediction.

Significant progress has been made in understanding the distribution of residual stress under UIT and the impact of fatigue-life prediction. For example, Yuan et al. [24] proposed a new three-dimensional numerical analysis method, which combines welding thermodynamic simulation with dynamic elastoplastic finite element analysis of welded joints. The residual stress distribution of the nonbearing cruciform joints before and after UIT is predicted by comparing the simulation and experimental results. Based on fracture mechanics, the fatigue strength of welded joints was also evaluated. The results show that this method can effectively distinguish the fatigue strength of welded and UIT-treated joints. Yang et al. [25] used the Navarro Rios model to study the fatigue resistance of

AISI304 stainless steel after ultrasonic shock treatment. The Johnson–Cook material model was used to establish the finite element model to obtain the required residual stress and dent profile, and the damage was considered. The results show that the fatigue performance of the material is improved with the increase in the coating. However, challenges and uncertainties still exist. Firstly, the research focused on the experimental test of fatigue performance or the numerical simulation of a single link, lacking the integrated whole process simulation from welding thermal mechanical coupling–residual stress evolution–UIT stress regulation–fatigue-life prediction, and the quantitative correlation between welding process and UIT fatigue performance was not established; secondly, there is a lack of research on welding UIT fatigue of high-strength steel such as qste700tm. The existing simulation models mostly use the constitutive and heat-source parameters of general steel, and lack of accurate parameter calibration suitable for the steel, resulting in limited prediction accuracy; third, the existing research has not systematically compared the stress distribution and fatigue-life differences in welding/non welding and UIT/non-UIT working conditions, revealed the insufficient coupling effect of welding residual stress and UIT process, and the simulation results lack multi-dimensional parameter sensitivity analysis and experimental statistical verification.

Based on the above background, relying on the finite element analysis software ABAQUS and the fatigue-life prediction platform FE-SAFE [26–29], taking QSTE700TM high-strength steel as the research object, a numerical simulation system for the whole process of fatigue properties of welded UIT specimens was constructed, and combined with experimental verification, the breakthrough and expansion of the existing research on fatigue properties of welded UIT were realized. The core innovation and promotion of this study are as follows: first, the full-chain simulation model of QSTE700TM high-strength steel from welding heat input to fatigue initiation is constructed for the first time, and the Goldak double ellipsoid heat-source calibration and Johnson–Cook constitutive optimization are integrated to realize the integrated prediction of welding residual stress evolution and UIT fatigue life, which makes up for the deficiency of single link simulation in the existing research; secondly, through trial-and-error method and sensitivity analysis, the heat source, constitutive and damping parameters of QSTE700TM were calibrated, which filled the parameter blank of numerical simulation of welding UIT fatigue of high-strength steel; thirdly, four groups of control experiments and simulations of welding/non-welding and UIT/non-UIT were designed to quantify the control mechanism of welding residual stress on the fatigue properties of UIT specimens, and to clarify the fatigue strengthening effect of UIT process under different stress levels; fourth, the reliability and reusability of the simulation model are improved through multi-parameter sensitivity analysis (JC constitutive, damping, heat source parameters) and statistical verification of fatigue experiments (mean, coefficient of variation, 95% confidence interval), which provides an accurate numerical method for fatigue-life prediction of high-strength steel welded UIT specimens in engineering.

2. Experimental Setup

2.1. Welding Test

QSTE700TM low alloy high strength steel was selected in the test, and its yield strength and tensile strength were 740 MPa and 888 MPa, respectively. The size is 300 mm × 150 mm × 2 mm, and the main components are shown in Table 1. Welding equipment cloo cruise gas-shielded welder, model QINEO PULSE 601 PRO (Kalklos Schwes Technology GmbH in Haigel, Germany). The welding material is WH80 gas-shielded welding wire produced by Wuhan Iron anchor (Gate 1, Changqian, Qingshan District, Wuhan City.) with a diameter specification of 1.2 mm. The welding voltage is 21 V, the welding current is 110 A, and the welding speed is 60 cm/min.

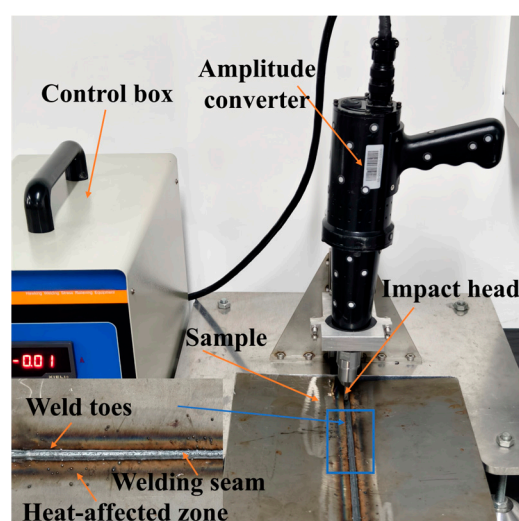
Table 1. Main chemical composition and mass fraction of QSTE700TM (wt.%).

	C	Si	Mn	P	S	Cr	Ni	Cu	Alt
QSTE700TM	≤0.12	≤0.60	≤2.1	≤0.025	≤0.015	≤0.15	≤0.15	≤0.2	≥0.015

2.2. Ultrasonic Impact Test

Hy2050 Haokeneng stress-relief equipment (Shandong Huayun Electromechanical Technology Co., Ltd., Jinan, China), was used for ultrasonic impact tests, which is mainly composed of an ultrasonic control box, ultrasonic horn and single-pin impact head, as shown in Figure 1. The control box converts 50 Hz AC into ultra audio AC, and the amplitude transformer amplifies the output amplitude. The working frequency is 20 kHz, and the maximum output amplitude in the test is 50 μ m, so as to meet the requirements of ultrasonic shock processing.

In the experiment, the single-needle impact head was used to conduct UIT on the surface of the thin plate, and the diameter of the impact head was 6 mm. During the impact process, the impact head of the impact gun was aligned with the surface of the thin plate to be treated and was basically perpendicular to the surface of the part. When carrying out ultrasonic shock treatment, we used the impact gun to slightly exert a certain force on the surface of the thin plate, so that the impact gun could carry out UIT on the surface of the thin plate under the action of dead weight, and try to ensure that all the surfaces to be treated were impacted during the treatment process.

**Figure 1.** Ultrasonic impact test setup.

2.3. Residual Stress Measurement Test

The residual stress of the sample was measured by X-ray diffractometer (XRD) (Proto Manufacturing Ltd. in LaSalle, Canada), as shown in Figure 2. A 1 mm circular collimator was used for stress measurement. First, the X-ray was emitted from the X-ray source and scattered to the measurement point on the sample surface through the emitter. The diffraction occurred on the metal lattice, and the diffracted X-ray entered the counter tube through the receiving hole for recording. By recording the diffraction spectra of different angles, their peaks were analyzed.

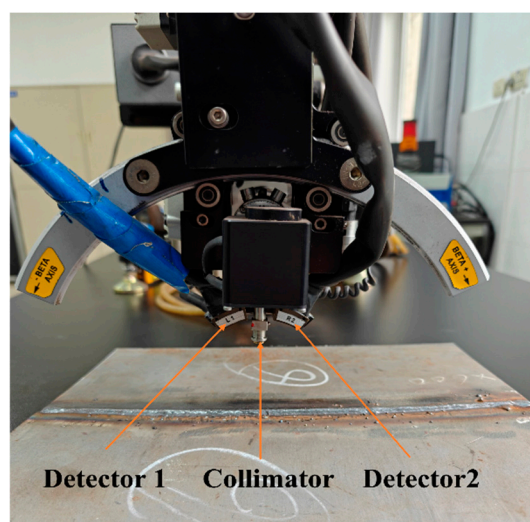


Figure 2. X-ray residual stress measurement.

2.4. Fatigue Test

As the test material, 2 mm QSTE700TM low-alloy high-strength steel was selected, and the fatigue samples were obtained by wire-cutting method. The preparation and specific dimensions of fatigue samples are shown in Figure 3, with a thickness of 2 mm. Considering the consistency of fatigue test results of multiple groups of samples, 2–3 samples were selected for each group.

The average room-temperature tensile properties of QSTE700TM high-strength steel measured in this study were tested on three repeated standard tensile specimens according to the GB/T 228.1-2021 standard[30]; the average values of tensile performance data are shown in Table 2. Its yield strength and tensile strength were 732 MPa, 750 MPa, 738 MPa and 875 MPa, 893 MPa, 896 MPa, respectively. The standard deviations are 9.17 MPa and 11.36 MPa, respectively.

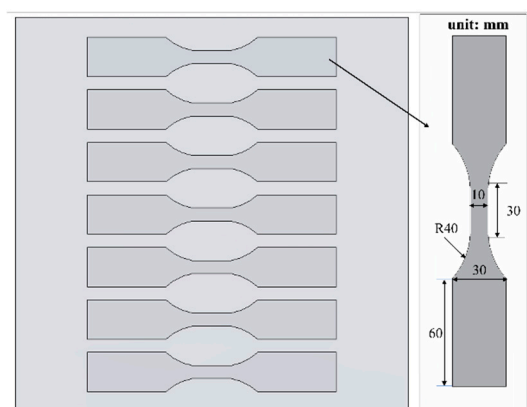


Figure 3. Fatigue specimen.

Table 2. Room-temperature tensile properties of QSTE700TM low-alloy high-strength steel.

Yield Strength/MPa	Tensile Strength/MPa	Elongation	Young's Modulus/GPa
740	888	0.228	211

The frequency and amplitude of ultrasonic impact were set at 20 kHz and 50 μ m for welded and unwelded specimens, respectively. According to the above ultrasonic impact process, the current is 2.5 A and the impact time is 3 min. The impact area is shown in Figure 4. During the impact process, we ensured that the impact gun was vertical to the impact surface, moved horizontally and reciprocally along the impact surface, and conducted repeated impact treatments. The impact test was completed by the dead weight of the impact gun without additional pressure on the arm.

The method of grinding the weld reinforcement was to use a grinder to grind the welded joint, and completely grind the weld reinforcement to be flush with the base metal, so as to eliminate the stress concentration. The samples were divided into four groups, as shown in Figure 5, including welded unUIT samples, unwelded unUIT samples, welded UIT samples and unwelded UIT samples. For each group of samples that needed ultrasonic, we ground them with a grinder after cutting. Then we conducted the UIT, and finally ground to a smooth surface.

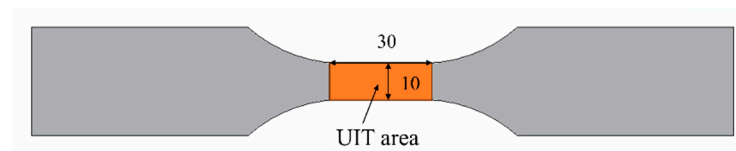


Figure 4. UIT area.

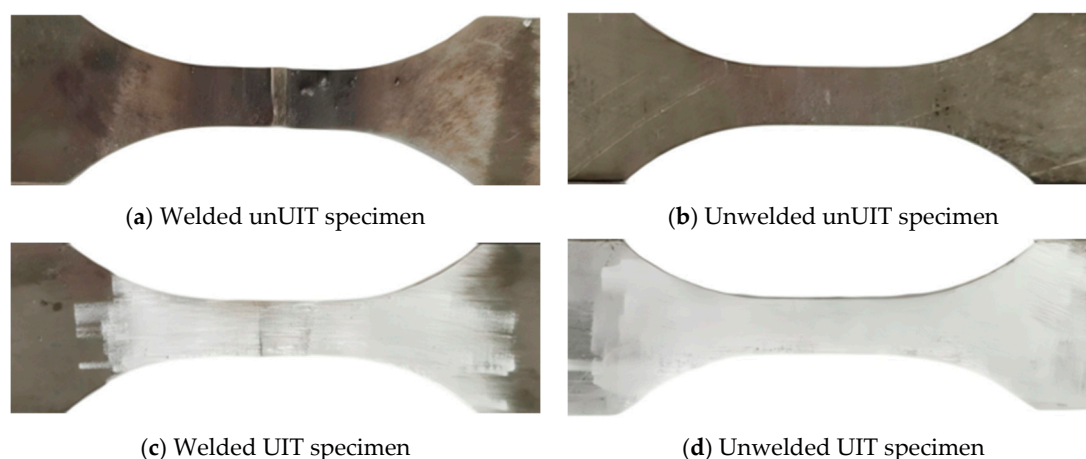


Figure 5. Comparison of four fatigue styles.

In this paper, the tensile fatigue test of 700 MPa low alloy high strength automotive steel QSTE700TM was carried out by using HDT105B electro-hydraulic servo fatigue testing machine (Shenzhen Wance Testing Machine Co., Ltd., Shenzhen, China). Load control, sinusoidal waveform, test frequency 30 Hz, stress ratio 0.1. For test-process control and test-data processing, refer to the national standard GB/T3075-2021 [31] “control method of axial force in fatigue test of metallic materials” and GB/T24176-2009 [32] “statistical scheme and analysis method of fatigue test data of metallic materials”. Refer to GB/T13816-92 [33] pulsating tensile fatigue test method for welded joints for test process control and test data processing of welded specimens. Ultrasonic modification of low-alloy automotive steel weldments was carried out by using an optimized ultrasonic process. Sample clamping is shown in Figure 6.

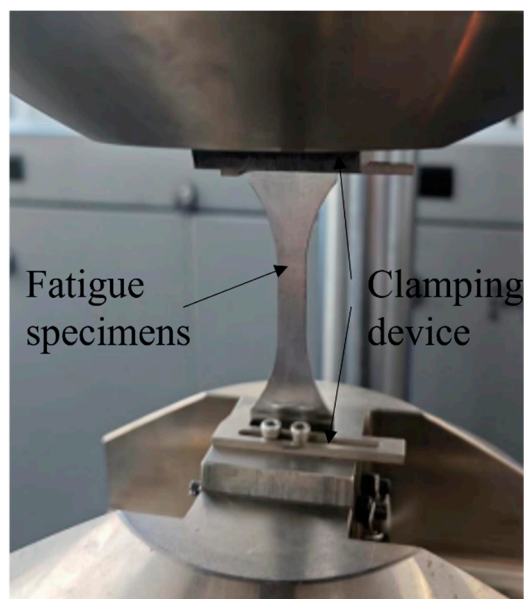


Figure 6. Fatigue-test specimen clamping.

2.5. Microscopic Experiment at the Fracture

After the fatigue test, the fracture morphology of QSTE700TM fatigue sample was detected by using Guoyi quantum field emission scanning electron microscope SEM5000Pro (CIQTEK Co., Ltd., Hefei, China) equipped with energy dispersive spectrometer (EDS, X-MaxN 50, Oxford Instruments plc, High Wycombe, UK)). The sample was etched in corrosive solution (CuSO_4 : HCl : H_2O = 4 g: 20 mL: 20 mL), 30 s before observation.

3. Finite Element Model

The evaluation of residual stress field near welding details is the key to evaluating the improvement of fatigue strength of welded joints. In this study, ABAQUS was used to simulate uncoupled thermomechanical welding and explicit dynamics method was used to simulate the numerical simulation of UIT process. The fatigue life of specimens before and after UIT was simulated by FE-SAFE fatigue-analysis module. The flow chart is shown in Figure 7.

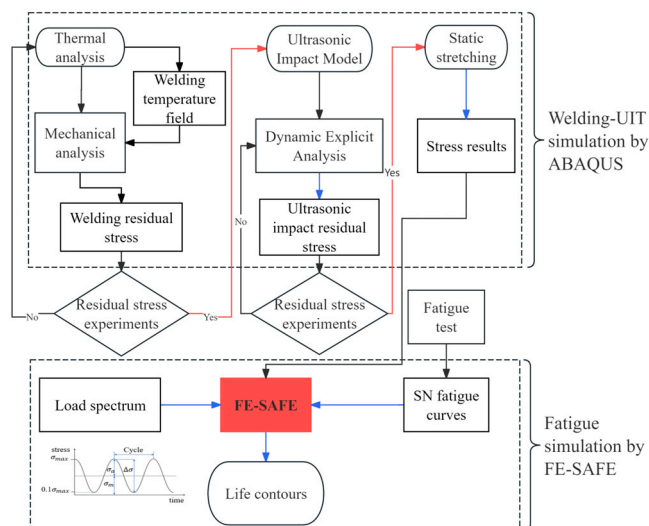


Figure 7. Welding-UIT-fatigue process simulation flow chart.

3.1. Welding Simulation

In this paper, a three-dimensional welding-plate model is constructed. The plate specification is 300 mm long, 300 mm wide and 2 mm thick. The weld height is set at 4 mm, the surplus height is 1 mm, and only one welding is carried out. Because the welding process involves the internal heat conduction of the structure and the heat exchange with the external environment, the situation is more complex. Reasonable meshing is very important for accurate and efficient calculation of welding process. This can not only ensure the calculation accuracy but also improve the calculation efficiency. In the mesh generation of the model, the numerical calculation process of the weld and heat-affected zone is more complex. Fine mesh generation is adopted, and the minimum element size is $0.2\text{ mm} \times 0.2\text{ mm} \times 0.2\text{ mm}$. In the area far away from the weld, the temperature is less affected, so sparse mesh generation is adopted. See Figure 8 for overall mesh division and local mesh refinement of welds. According to statistics, the model is divided into 176,800 units, including 196,176 nodes. When conducting three-dimensional heat transfer simulations, the heat transfer hexahedral element (DC3D8) was selected as the finite element. In the calculation and analysis of the welding stress field, the eight-node reduced integral three-dimensional stress element (C3D8R) was selected as the finite element for the calculation of the stress field. The meshing shape of the finite element of the stress field is completely consistent with that of the temperature field.

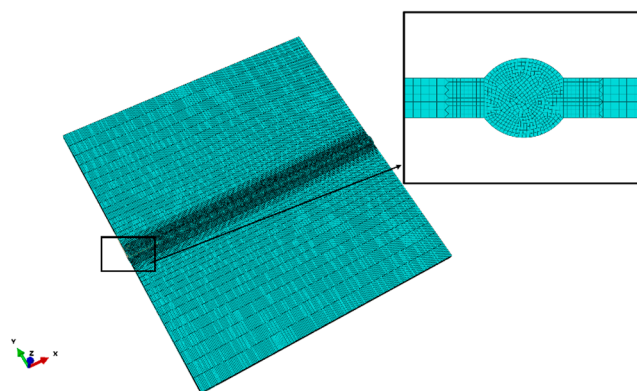


Figure 8. Flat plate welding finite element mesh.

The welding material was QSTE700TM low-alloy high-strength steel, and the thermophysical properties of weld metal and base metal were the same. The welding process was a nonlinear transient heat-transfer process, so the nonlinear effect of temperature should be considered when defining the thermophysical properties of materials. The required material parameters include thermal conductivity λ , density ρ and specific heat capacity C . The relationship between material parameters and temperature is shown in Table 3.

Table 3. Thermophysical parameters of QSTE700TM low-alloy high-strength steel material.

Temperature T (°C)	Thermal Conductivity λ (W/m·K)	Density ρ (kg/m ³)	Specific Heat Capacity C (J/kg·K)
20	50	7850	460
200	45	7840	525
400	41	7830	570
600	33	7820	620
800	27	7810	675
1000	23	7800	730
1200	18	7790	770

During the welding process, the material will experience the process of local rapid temperature rise and rapid cooling. The welding heat source is not only locally concentrated, but also in a state of instantaneous movement. This makes it easy to form an uneven field with sharp temperature gradient changes in the weldment, which will lead to greater stress and deformation. Therefore, reasonable selection of the heat-source model is very important for simulating welding temperature field and accurately calculating stress and strain. The double ellipsoid heat-source model proposed by Goldak [34] can effectively deal with the problem of the temperature gradient change in the welding arc at the front and rear ends of the weld pool. The model consists of two quarter ellipsoids at the front end and two half ellipsoids at the back end. Compared with other models, it has obvious advantages in dealing with the change in temperature gradient in the welding process.

The expression for the heat source of the front half-ellipsoid is provided in the following Equation.

$$q(x, y, z) = \frac{6\sqrt{3}f_1Q}{\pi a_1 b c \sqrt{\pi}} \exp\left(-3\frac{x^2}{a_1^2}\right) \exp\left(-3\frac{y^2}{b^2}\right) \exp\left(-3\frac{z^2}{c^2}\right) \quad (1)$$

The mathematical expression for the heat source of the posterior half ellipsoid is presented in the following equation.

$$q(x, y, z) = \frac{6\sqrt{3}f_2Q}{\pi a_2 b c \sqrt{\pi}} \exp\left(-3\frac{x^2}{a_2^2}\right) \exp\left(-3\frac{y^2}{b^2}\right) \exp\left(-3\frac{z^2}{c^2}\right) \quad (2)$$

where a_1 , a_2 , b , and c are independent parameters, f_1 and f_2 represent the energy fractions of the anterior and posterior hemispheres, respectively, satisfying $f_1 + f_2 = 2$.

To ensure the accuracy of the heat-source model, the trial-and-error method was used to calibrate the heat-source parameters based on actual welding-process parameters and publicly available literature on the welding of high-strength steel of the same grade as QSTE700TM [35]. The heat-source parameters were iteratively adjusted using the QSTE700TM welding-process window as a constraint until the simulated melt temperature field matched the process requirements completely. The final calibration parameters obtained were $a_1 = 3$, $a_2 = 6$, $b = 2.5$, $c = 1.2$, $f_1 = 0.6$, and $f_2 = 1.4$. This parameter is suitable for the QSTE700TM welding process used in this study, and the range of heat-source parameters for arc welding is consistent, with good process adaptability and literature basis.

In order to verify the rationality of the calibrated heat-source parameters, the characteristics of the melt-pool temperature field were validated. The width and depth of the molten pool were 4.6 mm and 2 mm respectively. The size of the welding area was consistent with the typical range of high-strength steel of the same grade welding simulation, indicating that the calibrated heat-source parameters can accurately characterize the welding heat input characteristics in this study and provide reliable thermal boundary conditions for subsequent welding thermal-mechanical coupling simulations.

The selection of welding heat source determines the accuracy of simulation results. Under the condition of the same welding heat input, the criterion to judge the rationality of welding heat source is whether the simulated weld pool boundary is consistent with the actual weld fusion line. In this paper, the double ellipsoid heat source model is selected for simulation, and the DFLUX user subroutine is used to load the heat-source movement process, and the element birth and death technology is used to edit the Model Change command to realize the weld-filling process.

In the calculation of welding temperature field, in addition to the heat-input effect of the arc on the weldment, the effects of initial preheating of the welding structure, its own heat conduction, radiation and convection of the surrounding environment should also be considered. In the thermal analysis, the ambient temperature is set at 20 °C, the

convective heat-transfer coefficient is assumed to be 20 W/m²·K, the emissivity (ε) is 0.75, and the Stefan–Boltzmann constant (σ) is 5.67×10^{-8} W/m²·K⁴.

In the finite element analysis of welding stress field, according to the thermal elastoplastic theory, the performance parameters of QSTE700TM low-alloy high-strength steel have a nonlinear relationship with temperature. It is necessary to define material parameters at different temperatures, including linear expansion coefficient α , elastic modulus E , Poisson's ratio μ , etc., as shown in Table 4.

Table 4. QSTE700TM low-alloy high-strength steel material mechanical property parameters.

Temperature T (°C)	Thermal Expansion α (/°C)	Young's Modulus E (MPa)	Poisson's Ratio μ	Yield Strength σ_y (MPa)	Density ρ (kg/m ³)
20	1.15×10^{-5}	2.10×10^5	0.29	740	7850
200	1.22×10^{-5}	1.86×10^5	0.30	680	7840
400	1.38×10^{-5}	1.64×10^5	0.31	640	7800
600	1.46×10^{-5}	1.42×10^5	0.32	540	7780
800	1.51×10^{-5}	1.08×10^5	0.33	320	7760
1000	1.68×10^{-5}	7.7×10^4	0.34	80	7750
1200	1.72×10^{-5}	4.4×10^4	0.34	15	7730
1400	1.85×10^{-5}	2.2×10^4	0.35	5	7680

The elastoplastic stress analysis in plate welding involves the influence of temperature-field distribution, thermal stress, elastic and plastic deformation, welding residual stress and thermal cycle on material behavior. The temperature gradient in the welding process leads to material expansion and contraction, resulting in a complex stress state. The thermal stress is caused by temperature difference, and the elastic and plastic deformation reflect the stress distribution in different regions. In the elastoplastic stress analysis, the total strain can be expressed as:

$$\varepsilon_{ij} = \varepsilon_{ij}^e + \varepsilon_{ij}^p + \varepsilon_{ij}^{th} \quad (3)$$

where ε_{ij}^e is the elastic strain, ε_{ij}^p is the plastic strain, and ε_{ij}^{th} is the thermal strain.

Thermal strain ε_{ij}^{th} can be expressed as:

$$\varepsilon_{ij}^{th} = \alpha(T - T^{ref}) \quad (4)$$

where α is the coefficient of thermal expansion of the material, T is the corresponding temperature at this time, and T^{ref} is the reference temperature.

In this simulation, the sequential-coupling method was adopted, and the temperature field results obtained in the previous calculation were applied as the temperature load in the stress field analysis. The simulated ambient temperature was set at 20 °C, which is equivalent to room temperature. In the actual welding process, in order to avoid rigid movement of weldment, constraint treatment must be implemented. In the process of welding stress field simulation and stress–strain analysis, the boundary conditions used are shown in Figure 9. The node at the top corner of 1 is constrained in X, Y and Z directions. For nodes with 2 vertex angles, constraints are implemented in Y and Z directions. For nodes with 3 and 4 vertex angles, constraints are only applied in the Z direction. This constraint method can not only effectively prevent the rigid body displacement of the structure in the calculation process, but also not excessively limit the free deformation of the weldment during welding, so as to ensure that the simulation results are as close to the actual welding conditions as possible.

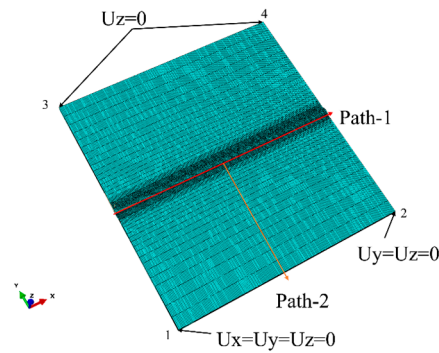


Figure 9. FEM displacement constraints and path selection.

3.2. UIT Simulation

The ultrasonic impact model is based on the numerical model after welding, and the results of welding residual stress of thin plate are transferred to ABAQUS/Explicit for subsequent calculation of UIT. The specific operation steps are as follows: first, we set the restart output request in the numerical model of welding QSTE700TM low-alloy high-strength steel, and submitted the analysis to get the calculation results. Then, in the subsequent UIT model, the deformed mesh was imported with the Import command, and the impact pin model was established by importing the post-weld residual stress field corresponding to the deformed mesh with the predefined field command.

The impact needle (the reference point RP-1 is shown in Figure 10) applied the velocity load in the negative direction of the Z axis (i.e., the direction of the plate thickness) and the displacement load along the X direction (the direction of the weld), so as to produce plastic deformation on the surface of the thin plate. The impact head with elastic modulus $E = 211$ GPa, Poisson's ratio $\nu = 0.3$ and measuring mass $m = 5.4$ g was set as a rigid body and consistent with the actual shape and size to simulate the tip with a diameter of 6 mm. The frequency of ultrasonic shock was 20 kHz sine wave displacement load, and the amplitude was 50 μm . For each impact, the displacement of the impact head along the X direction was 0.5 mm. And all degrees of freedom in other directions were limited to 0. The average initial impact velocity V_{ini} was approximately equal to the maximum velocity V_{max} at the output end [24]:

$$V_{ini} = V_{max} = 2\pi f_{u1}A \quad (5)$$

where f_{u1} is the frequency of the ultrasonic impact, and A is the maximum displacement amplitude of the waveguide output under loading conditions.

For the material model, the Johnson–Cook equation [36] was selected, considering work hardening and strain rate hardening effects to evaluate the stress–plastic strain relationship. The Johnson–Cook Equation is presented below.

$$\sigma = [A + B(\varepsilon)^n] \left[1 + C \ln \left(\frac{\dot{\varepsilon}}{\dot{\varepsilon}_0} \right) \right] \left[1 - \left(\frac{T - T_0}{T_m - T} \right)^m \right] \quad (6)$$

where σ is the applied stress; ε is the equivalent plastic strain; $\dot{\varepsilon}$ is the equivalent plastic strain rate; $\dot{\varepsilon}_0$ is the reference strain rate; A is the initial yield stress, which is set to 740 MPa; B is the strain-hardening parameter, which was set to 700 MPa; C is the intensification parameter dependent on strain rate, which was set to 0.014; n and m are the work-hardening exponent and thermal softening coefficient, which were set to 1.03 and 0.26 respectively; T_0 , and T are the reference temperature, material melting temperature, and current temperature, respectively. Since the temperature increase in the impact experiment was small, the effect of temperature was neglected.

In order to verify the rationality of the above parameters, the single-factor sensitivity-analysis method was used to conduct $\pm 20\%$ disturbance analysis on the key parameters B , n , C and m in ABAQUS. Table 5 lists the influence of parameter variation on peak equivalent stress and maximum plastic strain. The results show that when the parameters fluctuate within $\pm 20\%$, the maximum rate of change in the peak equivalent stress is 3.70%, and the maximum rate of change in the maximum plastic strain is 4.44%. The variation range of all key output indicators is less than 5%, indicating that the JC parameters selected in this paper are not sensitive to the impact of simulation results. Therefore, in the absence of special JC parameters for the material, it is reasonable and reliable to use the parameter combination of this study for finite element analysis of ultrasonic impact process.

Table 5. Johnson–Cook parameter-sensitivity analysis results.

Parameter	Range of Change	Peak Equivalent Stress /MPa	Stress Variation Rate/%	Maximum Plastic Strain	Strain Rate of Change/%
Benchmark	0	920	—	0.225	—
B	−20%	897	2.50%	0.220	2.22%
B	+20%	943	2.50%	0.219	2.66%
n	−20%	954	3.70%	0.215	4.44%
n	+20%	891	3.15%	0.218	3.11%
C	−20%	914	0.65%	0.223	0.89%
C	+20%	926	0.65%	0.227	0.89%
m	−20%	936	1.74%	0.221	1.78%
m	+20%	911	0.98%	0.228	1.33%

To avoid unnecessarily long residual oscillations after a collision, which can sometimes lead to numerical instability, the integral material damping technique (mass proportional damping) is used as follows [24]:

$$C = \alpha M = 2\omega_0 \xi M \quad (7)$$

$$\omega_0 = \frac{\pi}{2T} \sqrt{\frac{E}{\rho}} \quad (8)$$

where C is the damping matrix of the FEM, M is the mass matrix of the FEM, α is the Rayleigh damping coefficient of the mass proportion, ω_0 is the minimum natural angular frequency, n is the corresponding damping ratio, $E = 210$ GPa is the elastic modulus, $\rho = 7850$ kg/m³, and $T = 2$ mm are the density and thickness of the specimen, respectively.

For the explicit dynamic simulation of QSTE700TM, severe local mesh deformation and high-frequency non-physical oscillation will occur in the impact area due to rapid thermal shock. If the damping is set too low, the calculation will not converge, or the results will appear as false fluctuations. Therefore, in this study, the damping ratio corresponding to the high-frequency mode is set as $\xi = 0.6$, which is mainly used to attenuate the high-frequency numerical noise, rather than to characterize the macro viscous damping of the material. This treatment is consistent with the existing literature on explicit simulation of high-strength steel welding [24].

In order to verify the influence of the damping ratio on the prediction of residual stress, damping sensitivity analysis was carried out: $\xi = 0.05$, $\xi = 0.3$ and $\xi = 0.6$ were selected to compare the peak value and distribution of longitudinal residual stress in the weld center after welding. The results show that the difference in residual stress peak value under the three working conditions is less than 3%, and the stress-distribution trend is completely consistent. It is proved that the setting of $\xi = 0.6$ only improves the stability

of numerical calculation and does not produce systematic deviation on the prediction results of residual stress.

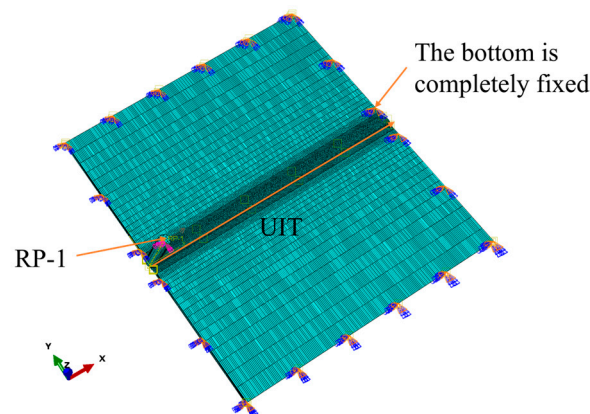


Figure 10. Impact simulation model.

3.3. Fatigue Simulation

Fatigue damage in structural materials subjected to cyclic loading is a cumulative evolutionary process [37,38]. Under the action of cyclic stress loads with different levels, the material incurs a certain degree of damage in each loading cycle; once the cumulative damage reaches a critical threshold, localized fatigue failure will occur in the structural component. Among all fatigue cumulative damage theories, the Miner linear cumulative damage theory [39] is currently the most extensively adopted one. This theory postulates that the fatigue damage induced by different stress levels is mutually independent, and the total damage of the material can be obtained through linear superposition. FE-SAFE 2022 software is developed on the basis of the linear cumulative damage theory combined with the S-N curve [40].

In this study, the finite element analysis software ABAQUS 2022 and the advanced fatigue durability analysis software FE-SAFE were jointly employed to simulate the fatigue life of QSTE700TM low-alloy high-strength steel specimens. First, the basic mechanical property parameters of QSTE700TM low alloy were acquired through laboratory experiments, and subsequent finite element analysis was conducted on the alloy via ABAQUS. The initial stress distribution of the specimens was derived by importing the residual stress field data of different specimen types into the finite element model. In the material model definition stage, the elastic modulus measured from the experiments and the stress–strain correlation of the alloy was input into ABAQUS. The finite element model was constrained by fixing one end of the specimen, while the other end was applied with a maximum stress consistent with that in the fatigue test. The model was meshed with 31,304 C3D8R linear hexahedral elements. On the basis of the ABAQUS finite element analysis results, load information and fatigue-related parameters were input into FE-SAFE to conduct quantitative analysis on the fatigue life of QSTE700TM low alloy under various fatigue-loading conditions. The selection of a suitable fatigue algorithm is a key step in fatigue simulation; according to the S–N curve obtained from the QSTE700TM low alloy fatigue experiments and the intrinsic mechanical properties of the material, the principal stress-based Morrow algorithm was selected for fatigue life calculation. The loading parameters set in FE-SAFE were completely consistent with the actual fatigue-test conditions. Four types of specimens were involved in the fatigue simulation, namely welded unUIT specimens, unwelded unUIT specimens, welded UIT specimens and unwelded UIT specimens.

Based on the results of the fatigue tests, the S–N curves reflecting the correlation between the maximum stress and the failure cycle number of the four aforementioned QSTE700TM low alloy specimens are presented in Figure 11. The experimental data points on the S–N curves can be fitted into a linear relationship using Equation (9), and the maximum stress corresponding to 10^7 loading cycles was defined as the fatigue limit of the specimens [41]. Consistent with the general fatigue-behavior law, the failure-cycle number of the specimens decreases gradually with the increase in stress amplitude. When the failure-cycle number of welded unUIT specimens, unwelded unUIT specimens, welded UIT specimens and unwelded UIT specimens reaches 10^7 , the corresponding maximum stress values are 350 MPa, 390 MPa, 448 MPa and 560 MPa, respectively. At this stress level, the S–N curves tend to be horizontal, which represents the fatigue limit of the material under the stress ratio $r = 0.1$. The relationship between the maximum stress and the failure cycle number can be well described by the power-law fitting method, and Basquin proposed a classic power-law model for this correlation as follows:

$$\sigma_{max}^m N_f = C \quad (9)$$

By taking the common logarithm of both sides of Equation (9), the double logarithmic linear equation (Equation (10)) for the maximum stress and the failure cycle number is derived:

$$m \log_{10} \sigma_{max} + \log_{10} N_f = \log_{10} C \quad (10)$$

In the above equations, σ_{max} denotes the maximum applied stress, m is the fatigue strength exponent of the material, N_f represents the fatigue failure cycle number, and C is a material constant that synthetically characterizes the overall fatigue performance of QSTE700TM low alloy. On the basis of the comprehensive fatigue test results, the quantitative relationships between the maximum stress and the failure cycle number for the four types of QSTE700TM low alloy fatigue specimens can be expressed as follows:

$$\text{Welded unUIT: } \log_{10} \sigma_{max} = -0.074 \log_{10}(N_f) + 3.06 \quad (11)$$

$$\text{Unwelded unUIT: } \log_{10} \sigma_{max} = -0.082 \log_{10}(N_f) + 3.16 \quad (12)$$

$$\text{Welded UIT: } \log_{10} \sigma_{max} = -0.057 \log_{10}(N_f) + 3.05 \quad (13)$$

$$\text{Unwelded UIT: } \log_{10} \sigma_{max} = -0.057 \log_{10}(N_f) + 3.1 \quad (14)$$

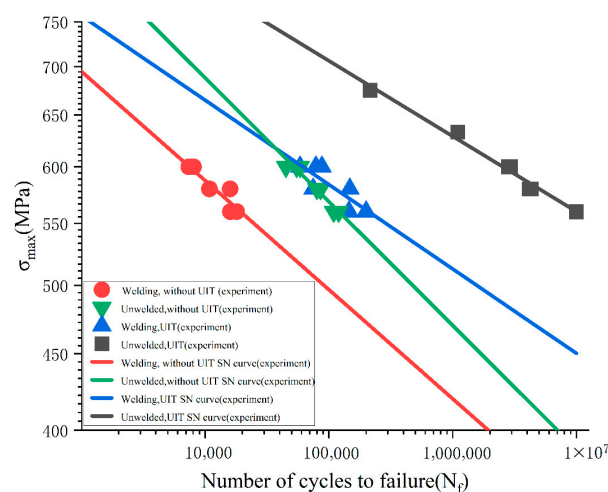


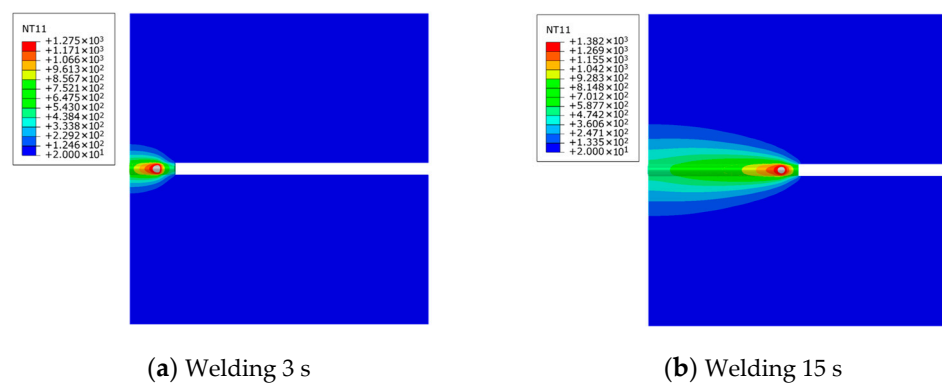
Figure 11. S–N plot of maximum stress versus number of cycles to failure.

It should be noted that the relaxation effect of welding residual stress during fatigue loading is not considered in this study, which is one of the main limitations of this study. In this paper, the numerical model focuses on the welding thermal mechanical coupling process of QSTE700TM high-strength steel and the initial distribution of residual stress, and the core of the analysis is the effect of residual stress on the fatigue-initiation stage. Under the long-term cyclic fatigue load, the cyclic plastic deformation of the material will lead to the gradual relaxation of the residual stress. In the subsequent research, Chaboche and other cyclic constitutive models can be introduced to carry out the quantitative analysis of the evolution and relaxation of the residual stress under the fatigue load and further improve the simulation system.

4. Results and Discussion

4.1. Welding Simulation Results

The simulation procedure for the welding temperature field mainly consists of three sequential stages. First, the weld element is reactivated from the deactivated state to mimic the filler-metal deposition and molten-pool formation. Then, the moving 3D heat source is applied to simulate the welding heating process, with the welding speed set at 10 mm/s. Finally, the welded joint undergoes natural cooling until the entire structure reaches room temperature. Based on finite element simulation, the instantaneous temperature contours of the low-alloy steel welding process are obtained and presented in Figure 12. At the center of the moving heat source, the peak temperature reaches 1425 °C. With increasing distance away from the weld center, the temperature gradually decreases to 20 °C. The temperature isotherms exhibit a closed elliptical pattern, with dense distribution ahead of the heat source, indicating a large temperature gradient in this region. In contrast, the isotherms behind the heat source are relatively sparse, corresponding to a milder temperature gradient. As the heat source moves continuously, the heated zone gradually expands. At the initial welding stage, the temperature at the heat-source center rises rapidly and exceeds the material melting point, leading to a sharp internal temperature gradient within the plate. After a period of stable welding, the heat input and heat dissipation reach an approximate balance, and the temperature field enters a quasi-steady state. When the entire welded structure is finally cooled to room temperature (20 °C), the temperature distribution becomes uniform, and the isotherms are approximately concentric, as clearly illustrated in Figure 12d.



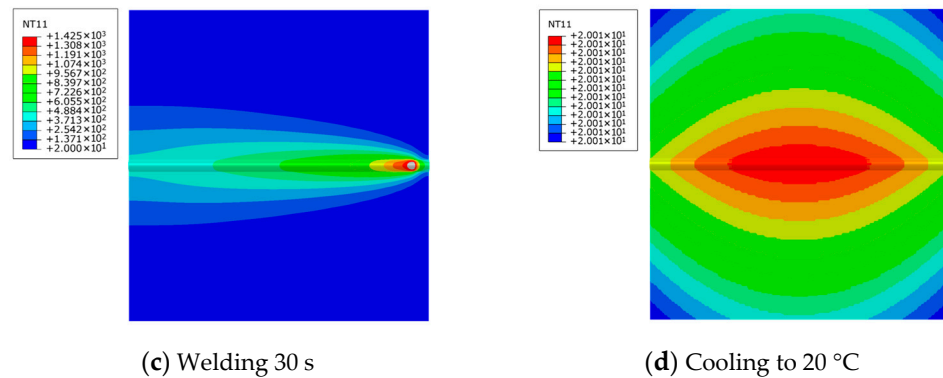


Figure 12. Welding temperature field.

The distribution characteristics of longitudinal (parallel to the weld, S11) and transverse (perpendicular to the weld, S22) residual stresses in the cooled low-alloy steel welded joints were further analyzed. During welding, uneven thermal expansion and contraction induce elastic-plastic deformation, which is the primary cause of residual stress. At high temperatures, plastic deformation occurs in the region surrounding the molten pool; this deformation cannot be fully recovered during cooling, resulting in the formation of internal residual stress. Figure 13 displays the welding residual stress contours, among which the Mises equivalent residual stress contour reflects the overall stress state of the welded joint. It can be observed that the equivalent residual stress in the weld and heat-affected zone is relatively high, with the maximum value approaching the material yield strength. By contrast, the equivalent residual stress in regions far from the weld is relatively low. For longitudinal residual stress, the middle part of the weld and its adjacent affected zone exhibits a tensile stress state, with a peak value of 708.4 MPa, exceeding the material yield strength. To satisfy the internal force equilibrium, longitudinal compressive stress appears at both ends of the weld, with a maximum value of 361.2 MPa. Moreover, the longitudinal residual stress gradually decreases and approaches zero as the distance from the weld increases. In terms of transverse residual stress, the highest values are concentrated in the weld region, especially at the weld toe, where the maximum transverse tensile stress reaches 281.9 MPa. Transverse compressive residual stress exists at both ends of the weld, with a peak value of 611.5 MPa.

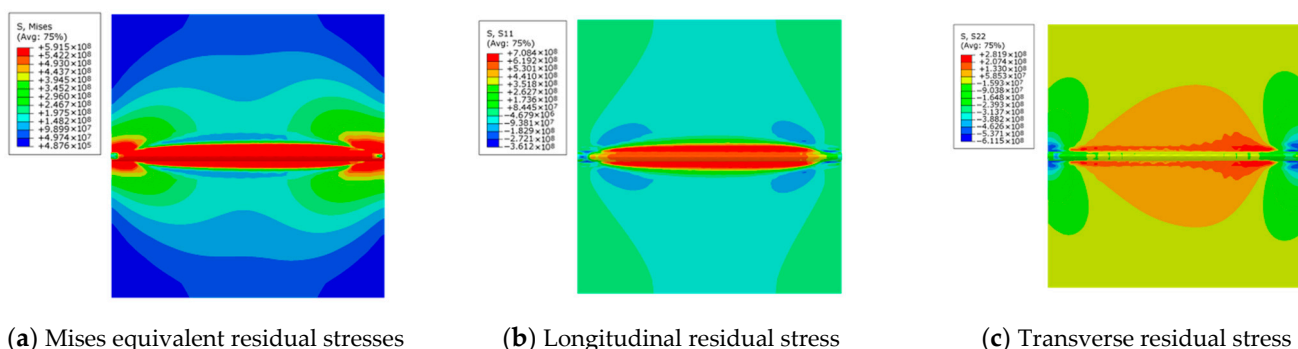


Figure 13. Welding residual stress cloud.

Two typical critical paths were selected for quantitative stress analysis, and their locations are shown in Figure 9. Path-1 is defined as the weld toe line on the upper surface of the welded joint, while Path-2 is the centerline perpendicular to the welding direction. According to the simulation results, the distribution curves of longitudinal and transverse residual stresses along Path-1 and Path-2 are plotted in Figure 14. During the welding

cooling stage, longitudinal shrinkage of the weld tends to cause outward bending of the two steel plates, showing a bow-like deformation tendency. This deformation is constrained by the surrounding base metal, resulting in tensile stress in the middle of the weld and compressive stress at both ends. Meanwhile, the cooling rate at both ends of the weld is noticeably higher than that in the middle region. Once the two ends finish shrinking first, the subsequent transverse shrinkage in the middle is restricted, which further induces transverse tensile stress in the central weld zone. In practice, the transverse residual stress is the combined result of these two mechanisms. In addition, during the welding heating stage, compressive plastic deformation occurs in the weld and its two sides. After cooling, the material in this region tends to shorten due to the earlier plastic deformation, but such shrinkage is restricted by the surrounding metal. As the weld metal returns to the elastic state, the earlier compressive deformation finally leads to the generation of longitudinal residual stress.

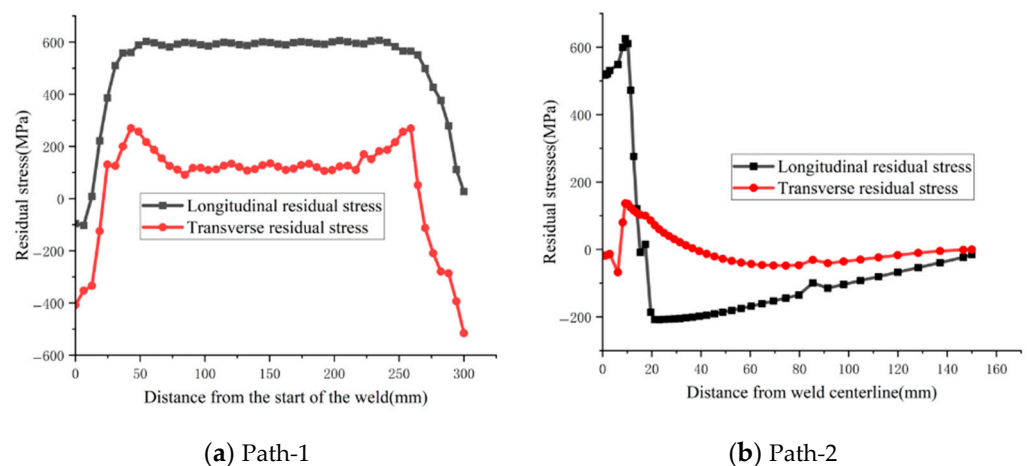


Figure 14. Welding residual stress distribution.

4.2. Simulation Results of the UIT Process

Figure 15 shows the change nephogram of longitudinal and transverse stress on the sample surface before and after UIT. By comparing the cloud images, it can be seen that UIT has obvious effect on reducing the residual stress on the surface of the sample. The Mises stress nephogram showed that after ultrasonic shock, most of the residual tensile stresses existing in the weld and heat affected zone were transformed into residual compressive stresses. UIT has a significant effect on the improvement of longitudinal residual stress, which greatly improves the original high-stress state in the weld area. In terms of transverse residual stress, after UIT, most of the residual stress in the impacted area is transformed into compressive stress, and the overall stress level is significantly reduced. UIT not only reduces the residual stress but also optimizes the stress distribution in the impacted area, making it more uniform. The original large stress gradient between the weld area and the two sides of the area was effectively alleviated, and there was no secondary stress concentration during the whole treatment process. This shows that ultrasonic treatment has a positive and reliable role in improving the stress state of welded joints.

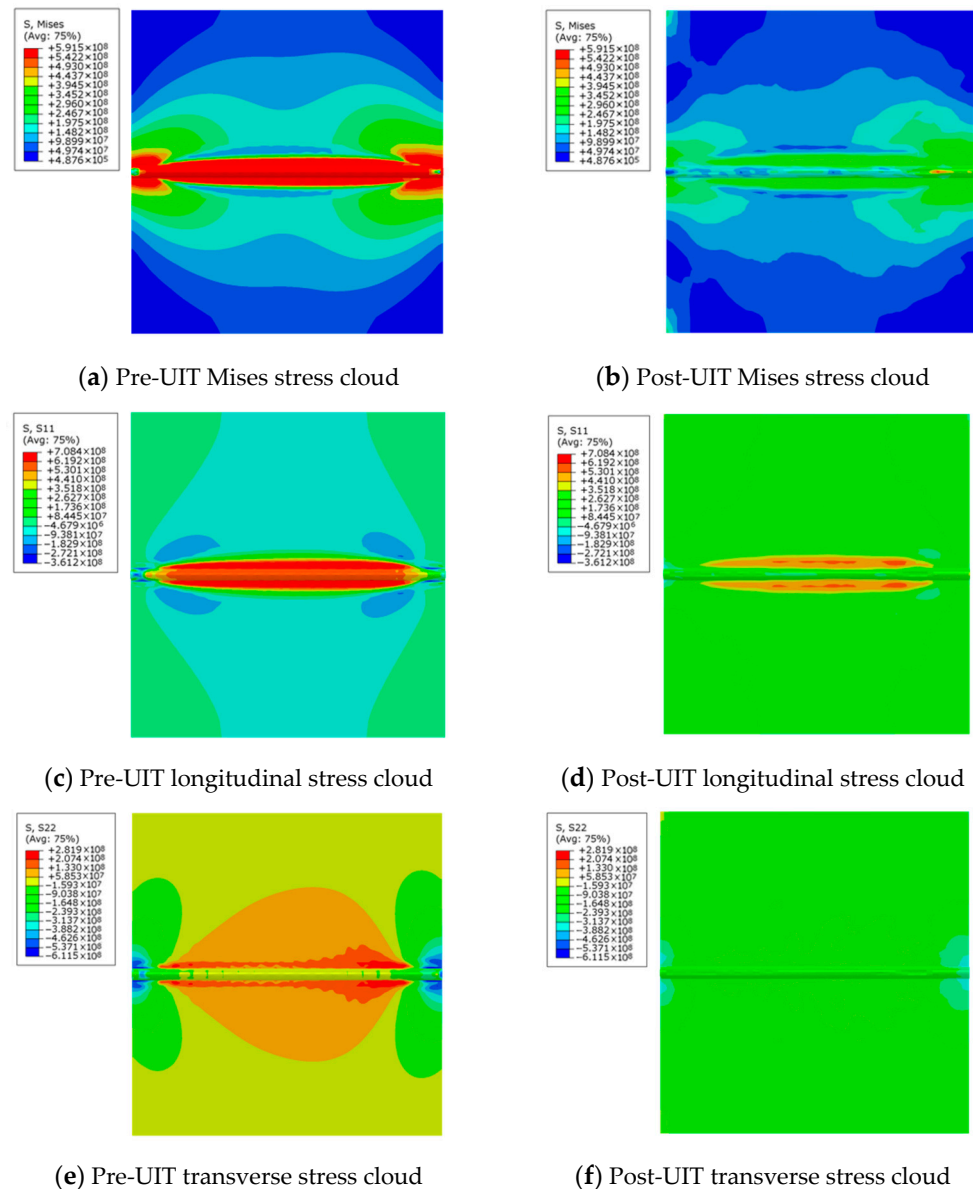


Figure 15. Stress distribution cloud before and after impact.

The samples before and after UIT were analyzed by X-ray diffraction [17] and compared with the simulation results. As shown in Figure 16, it can be observed that the longitudinal residual stress Figure 16a and transverse residual stress Figure 16b on path 1 (see Figure 9) were compared before and after UIT. After UIT, the residual stress in the impacted area decreased significantly. The residual stress in the weld area is reduced especially when it changes to the state of low tensile stress and compressive stress. In terms of longitudinal residual stress, the maximum longitudinal residual stress decreased from 606.5 MPa to 291.1 MPa, a decrease of about 52%. The average longitudinal residual stress decreased from 503.4 MPa to −102.3 MPa by about 120%, and the stress level decreased significantly. The maximum transverse residual stress decreased from 130.6 MPa to 73.9 MPa, a reduction of about 43.4%. The average transverse residual stress decreased from −35.3 MPa to −75.1 MPa by 113%. It can be seen that the longitudinal and transverse residual stresses are basically transformed into compressive stresses after UIT. This result is highly consistent with the finite element analysis and test data, which fully verifies the effectiveness of UIT in reducing welding residual stress.

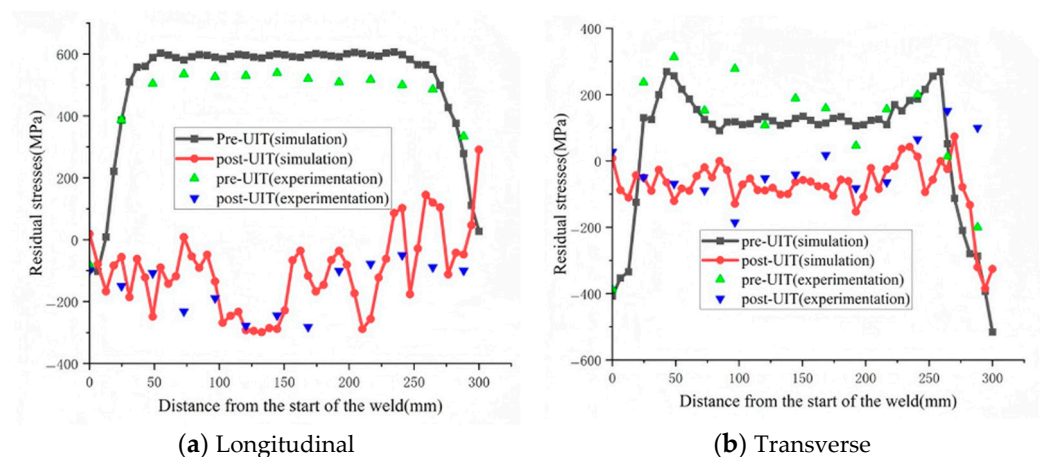


Figure 16. Residual stress distribution of Path-1 before and after UIT.

4.3. Fatigue Results

The fatigue life of QSTE700TM sample is obtained from the above fatigue test, as shown in Table 6. When the maximum stress is 600 MPa, the service life of welded unUIT specimen, welded UIT specimen, unwelded unUIT specimen and unwelded UIT specimen are 7629 cycles, 74,228 cycles, 59,120 cycles and 1,574,169 cycles respectively. When the maximum stress is 580 MPa, the service life of welded unUIT specimen, welded UIT specimen, unwelded unUIT specimen and unwelded UIT specimen are 12,817 cycles, 145,572 cycles, 79,764 cycles and 3,746,793 cycles, respectively. When the maximum stress is 560 MPa, the service life of welded unUIT specimen, welded UIT specimen, unwelded unUIT specimen and unwelded UIT specimen are 16,705 cycles, 188,851 cycles, 1,116,470 cycles and 10,000,000 cycles, respectively.

Considering the discrete characteristics of fatigue test, three repeated tests were carried out for each working condition. Due to the small sample size, the coefficient of variation (CV) was used to characterize the relative dispersion of the data, and the 95% confidence interval was calculated based on the t distribution to evaluate the reliability of the experimental results.

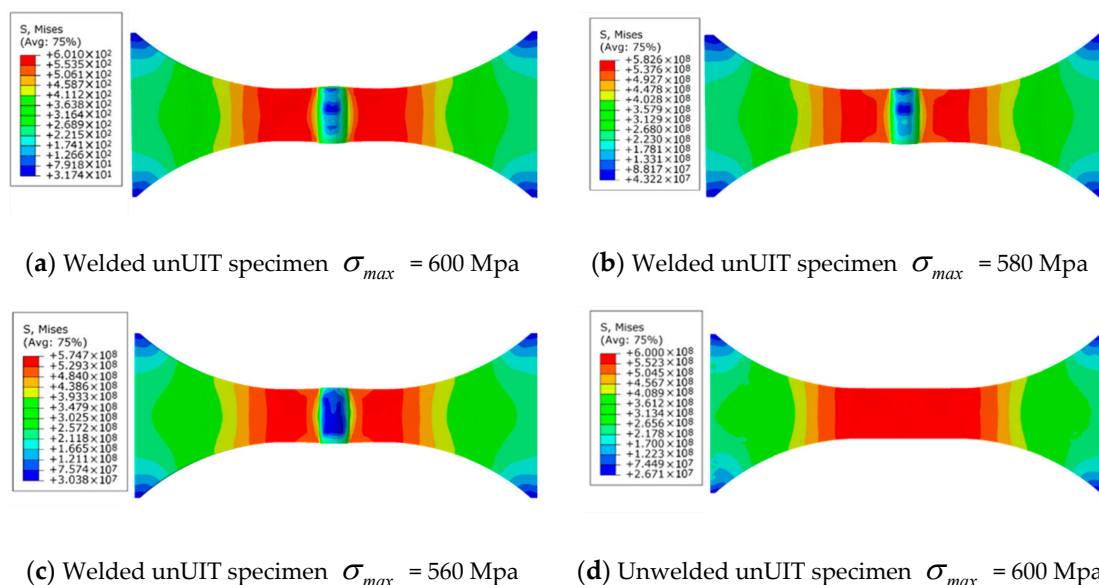
It can be seen from Table 6 that the coefficient of variation in the experimental data is less than 18%, indicating good repeatability of the experiment, and all the simulation prediction values are within the 95% confidence interval of the experimental data, which verifies the accuracy of the prediction results of the model in this paper.

Table 6. Fatigue-test life of specimen.

Specimen Type	Max Stress /MPa	Test 1	Test 2	Test 3	Test Average	Standard Deviation	Coefficient of Variation (CV, %)	95% Confidence Interval
Welded unUIT specimen	560	1.60×10^4	1.74×10^4	/	1.67×10^4	7.25×10^2	4.34%	$[1.02, 2.32] \times 10^4$
Welded UIT specimen	560	2.00×10^5	1.45×10^5	/	1.73×10^5	2.73×10^4	15.83%	$[-7.30, 41.83] \times 10^4$
Unwelded unUIT specimen	560	1.24×10^5	1.09×10^5	/	1.16×10^5	7.09×10^3	6.08%	$[5.28, 18.01] \times 10^4$
Unwelded UIT specimen	560	1.00×10^7	1.00×10^7	/	1.00×10^7	0	0	$[1.00, 1.00] \times 10^7$

Welded unUIT specimen	580	1.59×10^4	1.17×10^4	1.09×10^4	1.28×10^4	2.24×10^3	17.44%	$[6.02, 19.62] \times 10^3$
Welded UIT specimen	580	1.46×10^5	1.46×10^5	/	1.46×10^5	3.82×10^2	0.26%	$[1.43, 1.50] \times 10^5$
Unwelded unUIT specimen	580	8.56×10^4	8.01×10^4	7.36×10^4	7.98×10^4	4.93×10^3	6.18%	$[6.48, 9.48] \times 10^4$
Unwelded UIT specimen	580	4.14×10^6	4.13×10^6	/	4.13×10^6	1.94×10^3	0.05%	$[4.12, 4.15] \times 10^6$
Welded unUIT specimen	600	8.06×10^3	7.44×10^3	7.39×10^3	7.63×10^3	3.04×10^2	3.98%	$[6.70, 8.56] \times 10^3$
Welded UIT specimen	600	7.78×10^4	6.78×10^4	7.86×10^4	7.47×10^4	4.92×10^3	6.59%	$[5.97, 8.97] \times 10^4$
Unwelded unUIT specimen	600	5.49×10^4	5.36×10^4	6.89×10^4	5.91×10^4	6.92×10^3	11.70%	$[3.81, 8.02] \times 10^4$
Unwelded UIT specimen	600	3.01×10^6	2.64×10^6	/	2.83×10^6	1.86×10^5	6.56%	$[1.16, 4.49] \times 10^6$

The Mises equivalent stress contour is presented in Figure 17. The nominal cross-sectional area of the specimen gauge section is 20 mm², and the axial tensile stresses imposed on the right end of the QSTE700TM low-alloy steel specimens are set to 400, 450, 500, 560, 580 and 600 MPa, respectively. From the stress distribution, it can be observed that the stress level at the gripping end of the specimen is relatively low, while a pronounced stress concentration appears in the gauge section. The static finite element predictions agree well with the experimental measurements, validating that the numerical model can be reliably adopted for the subsequent fatigue-life evaluation.



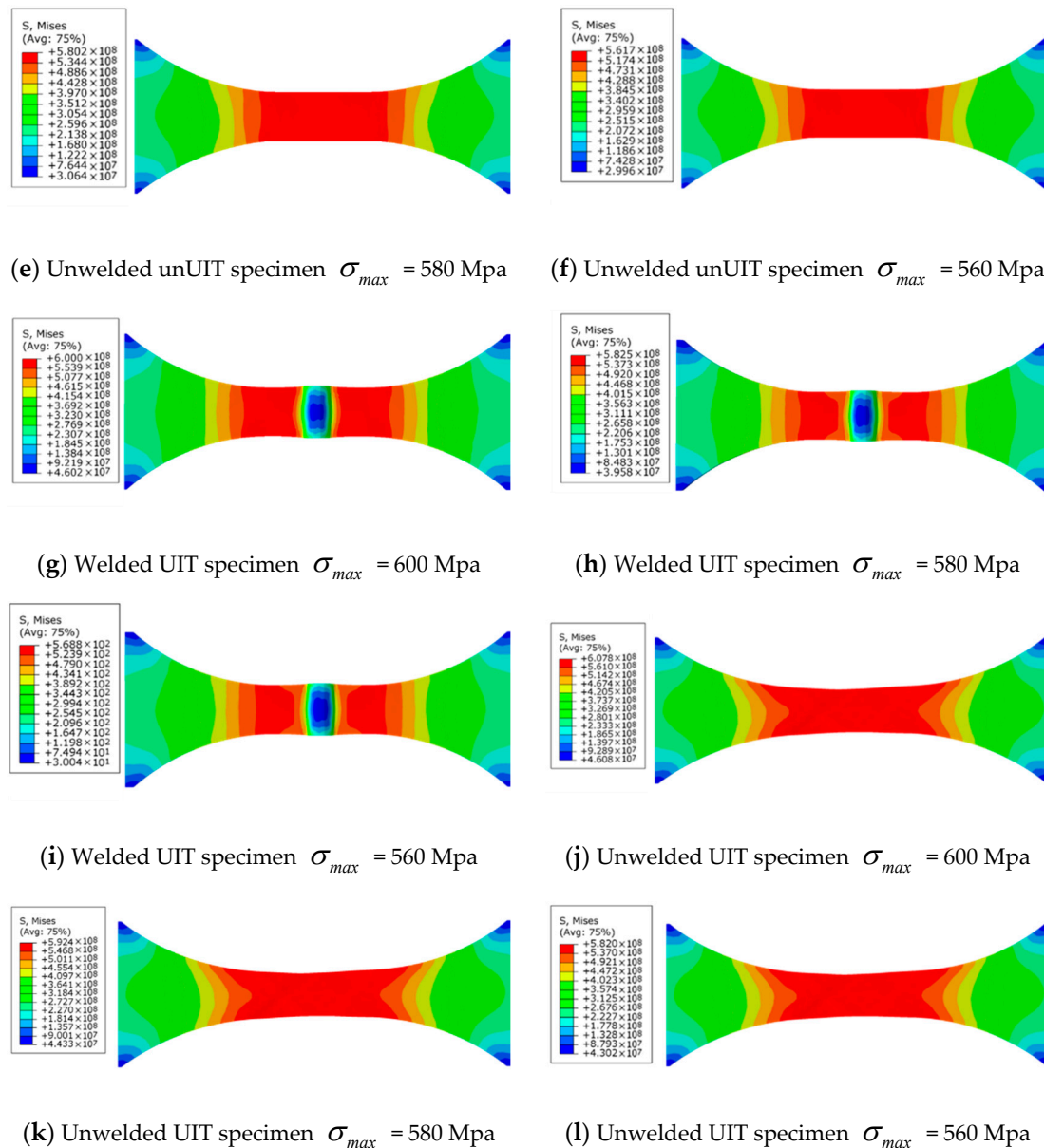


Figure 17. Stress cloud of fatigue specimen.

Figure 18 illustrates the contour plot of logarithmic fatigue life for QSTE700TM low-alloy steel specimens. Notably, the red-marked zones correspond to regions with degraded fatigue life and elevated failure risk. A consistent trend can be identified that fatigue life decreases monotonically with the increase in the maximum applied stress. For welded specimens, as depicted in Figure 18a–c, the regions with inferior fatigue life and high damage susceptibility are primarily concentrated at the weld toe and the heat-affected zone. For unwelded UIT specimens under relatively high maximum stress, the low-fatigue-life zone is initially located at the specimen center (Figure 18d) and gradually shifts toward the edge as the stress level rises (Figure 18f). In the case of unwelded UIT specimens, the regions with shortened fatigue life are mainly distributed in the transverse stress-concentration areas, as shown in Figure 18j,k; these critical zones also migrate toward the specimen edge under higher stress amplitudes. The remaining regions exhibit a fatigue life close to the “infinite life” criterion, and the upper fatigue life limit for QSTE700TM low-alloy steel specimens is defined as 10^7 cycles. The regions with relatively low fatigue life are found to occur on the interior surface of the specimen.

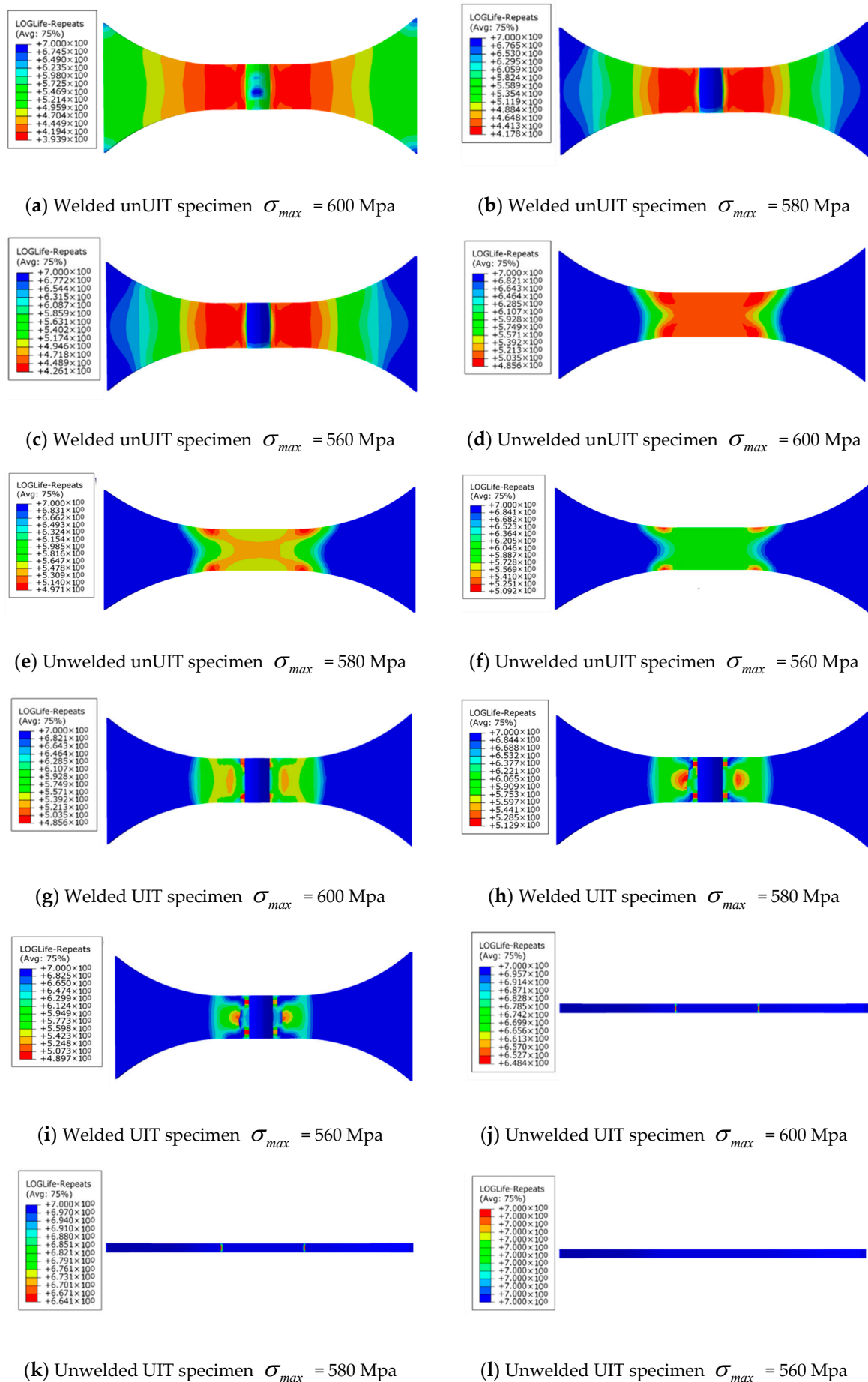


Figure 18. Fatigue log life distribution.

Specifically, the fatigue lives of different types of QSTE700TM low-alloy steel specimens under different maximum stress levels are as follows: the welded unUIT specimens have fatigue lives of 8689, 15,066 and 18,238 cycles when subjected to maximum stresses of 560, 580 and 600 MPa, respectively; the unwelded unUIT specimens exhibit fatigue lives of 71,695, 102,564 and 134,732 cycles under the same maximum stress levels; the welded UIT specimens correspond to fatigue lives of 77,924, 118,163 and 193,107 cycles when the maximum stresses are 560, 580 and 600 MPa, respectively; and the unwelded UIT specimens have fatigue lives of 4,315,190, 4,977,370 and 10,000,000 cycles under the maximum stresses of 560, 580 and 600 MPa, respectively.

A comparison between the experimental fatigue results and the corresponding numerical simulation outcomes of QSTE700TM low-alloy steel specimens is presented in Figure 19. The red data points represent the numerically predicted failure-cycle counts corresponding to six different maximum stress levels. The logarithmic fatigue-life contours under the maximum stresses of 560, 580 and 600 MPa are also displayed in the figure. On the basis of the simulated fatigue life data, the logarithmic S–N curves can be satisfactorily fitted. Alalkawi et al. [42] carried out UIT on 2017-T4 aluminum alloy specimens. Their results demonstrated that the fatigue strength of the specimens was enhanced by 4.16%, and the fatigue life was prolonged by 24% at 10^7 loading cycles. The maximum stress values corresponding to 10^7 cycles for welded unUIT, unwelded unUIT, welded UIT and unwelded UIT specimens are identified as 332 MPa, 392 MPa, 436 MPa and 560 MPa, respectively. After UIT, the fatigue limit of welded specimens increased by 31.3%, while that of unwelded specimens is raised by 42.9%.

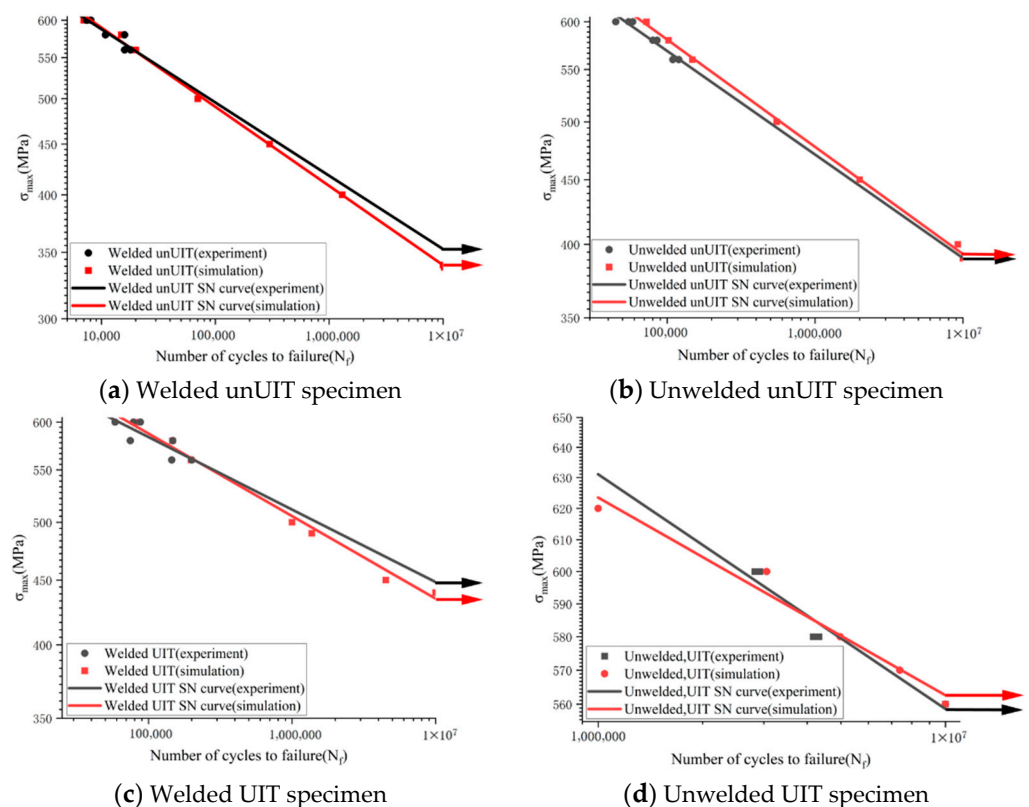


Figure 19. Test and numerical simulation S–N fatigue curves.

The quantitative relationship between the maximum stress obtained from numerical simulations and the corresponding number of failure cycles can be expressed as:

$$\text{Welded unUIT: } \log_{10} \sigma_{max} = -0.080 \log_{10} (N_f) + 3.03 \quad (15)$$

$$\text{Unwelded unUIT: } \log_{10}\sigma_{max} = -0.084\log_{10}(N_f) + 3.10 \quad (16)$$

$$\text{Welded UIT: } \log_{10}\sigma_{max} = -0.064\log_{10}(N_f) + 3.08 \quad (17)$$

$$\text{Unwelded UIT: } \log_{10}\sigma_{max} = -0.052\log_{10}(N_f) + 3.12 \quad (18)$$

Based on the quantitative comparison between experimental measurements and predicted results presented in Table 7, the proposed method demonstrates high precision in predicting the fatigue life of thin-plate materials. The deviation between experimental data and predicted values is controlled within 20%, confirming that the method can reliably describe the fatigue behavior of the actual material in fatigue-life estimation. In addition, compared with the traditional S–N fatigue curves obtained by nominal stress or hot-spot stress regression fitting, the present method shows distinct advantages in both prediction accuracy and computational efficiency. The adaptability and reliability of the method under various loading conditions are verified through comparison with measured fatigue-test data. Particularly at higher stress levels, the consistency between simulation results and experimental data is further improved, which validates the effectiveness of the method under extreme operating conditions.

Table 7. Comparison of sample test and predicted life.

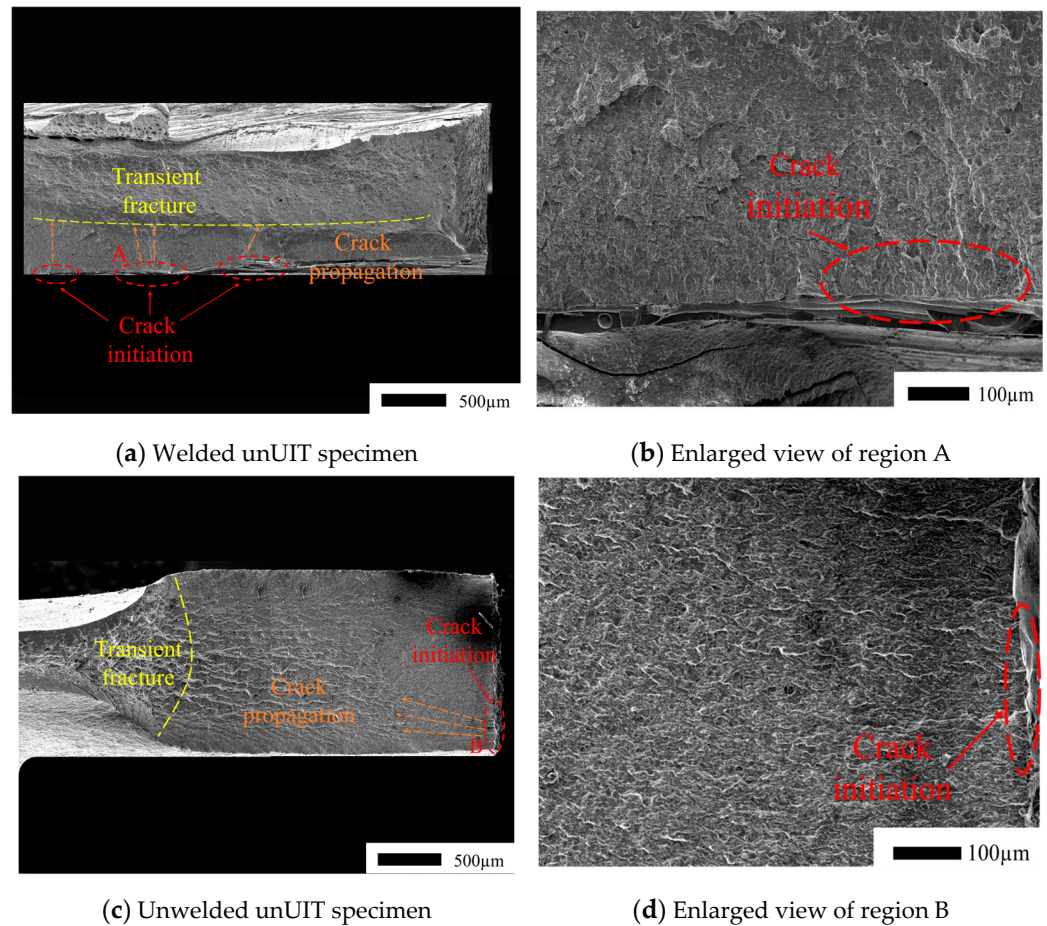
	Maximum Stress (MPa)	Stress Ratio	Predicted Results	Experimental Results	Error Percentage
Welded unUIT specimen	560	0.1	1.82×10^4	1.67×10^4	8.41%
	580	0.1	1.51×10^4	1.28×10^4	14.93%
	600	0.1	8.69×10^3	7.63×10^3	12.20%
Unwelded unUIT specimen	560	0.1	1.24×10^5	1.16×10^5	5.76%
	580	0.1	9.35×10^4	7.98×10^4	14.73%
	600	0.1	7.17×10^4	5.91×10^4	17.54%
Welded UIT specimen	560	0.1	1.93×10^5	1.73×10^5	10.59%
	580	0.1	1.35×10^5	1.46×10^5	8.53%
	600	0.1	7.89×10^4	7.47×10^4	5.30%
Unwelded UIT specimen	560	0.1	1.00×10^7	1.00×10^7	0
	580	0.1	4.38×10^6	4.13×10^6	5.50%
	600	0.1	3.05×10^6	2.83×10^6	7.24%

4.4. Fatigue-Fracture Morphology Analysis

Fatigue-crack initiation and early-stage propagation dominate the high-cycle fatigue life, collectively accounting for over 90% of the total service life. Consequently, investigating the underlying mechanisms of crack nucleation and propagation is pivotal for elucidating the fundamental nature of fatigue failure, diagnosing its root causes, and ultimately improving the fatigue resistance of materials [43]. The fatigue fracture morphologies of the various specimens are illustrated in Figure 20. As observed in Figure 20a,b, the fatigue-crack initiation sites (crack sources) of the welded unUIT specimens are located on the specimen surface, precisely at the convergence of radial fatigue striations, and exhibit a characteristic multi-source initiation behavior. Owing to the stress concentration effect induced by surface defects, fatigue cracks nucleate preferentially at these surface irregularities and subsequently propagate toward the material interior through mechanisms such as cleavage and dislocation slip. The resultant crack-propagation path is relatively linear, accompanied by the presence of a large cleavage facet. In contrast, Figure 20c,d reveal that the unwelded unUIT specimens also initiate cracks on the surface; however, they exhibit fewer crack sources, and the propagation zone is characterized by the

presence of smaller cleavage planes. For the welded UIT specimens (Figure 20e,f), the fatigue sources are found in the thinnest subsurface layer of the strengthened zone, with a notably tortuous crack propagation path. This transition of the crack source from the surface to the subsurface after UIT can be attributed to the formation of a high-hardness layer and severe plastic deformation on the specimen surface. This surface strengthening effectively suppresses the nucleation of surface-fatigue cracks, thereby forcing crack initiation to occur at internal material defects instead. The tortuosity of the crack path is further explained by the grain refinement induced by ultrasonic impact, which continuously alters the direction of crack propagation.

In summary, surface defects act as critical stress concentrators in both welded and unwelded specimens, triggering fatigue crack initiation and subsequent propagation into the material via cleavage and slip. After UIT, the specimen surface undergoes a series of beneficial changes: an increase in surface hardness, a denser grain structure in the welded zone, and the introduction of residual compressive stress. The residual compressive stress specifically reduces the driving force at the crack tip, impeding crack propagation. Collectively, these factors effectively delay or inhibit the formation of surface cracks, shift the crack source to the subsurface, and significantly extend the fatigue-crack initiation period.



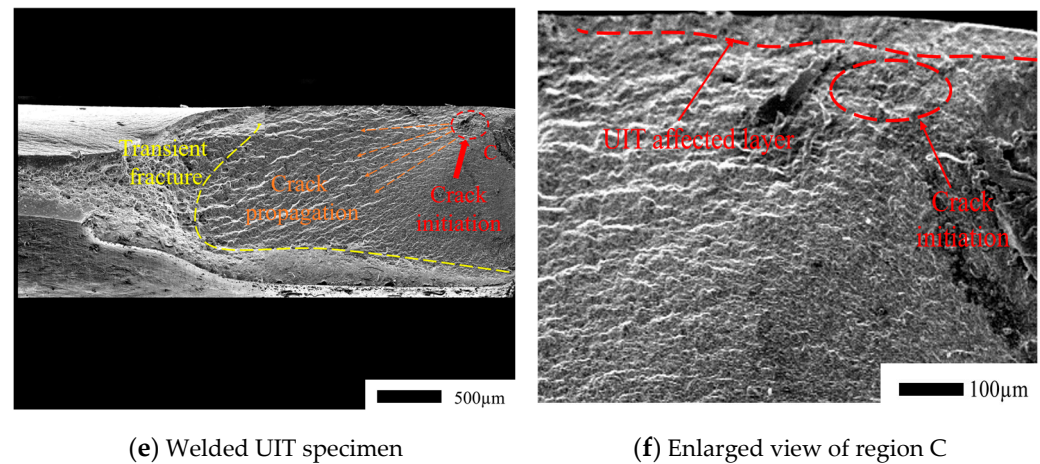


Figure 20. Fatigue-induced fracture morphology for different specimens.

Fatigue striations represent a typical characteristic in the fatigue-crack propagation region. Each stress cycle imposed by the fatigue-testing machine contributes to the formation of one fatigue striation. Variations in the fatigue-crack initiation site and the spacing of fatigue striations within the crack-growth region can be ascribed to the decrease in crack-driving force induced by dislocation accumulation [44,45]. As illustrated in Figure 21a–c, by comparing the micromorphology of each specimen at the middle part of the fatigue-crack propagation zone, it can be clearly observed that the spacing of fatigue striations in UIT-treated specimens is more compact than that in welded unUIT and unwelded unUIT specimens. Meanwhile, a large number of fine secondary fatigue striations exist between the relatively coarse fatigue striation bands in UIT specimens. Under the same stress ratio, finer and more tortuous fatigue striations on the fracture surface imply that more fatigue cycles are needed to reach the critical fracture condition, which corresponds to a higher fatigue life of the specimen [46,47].

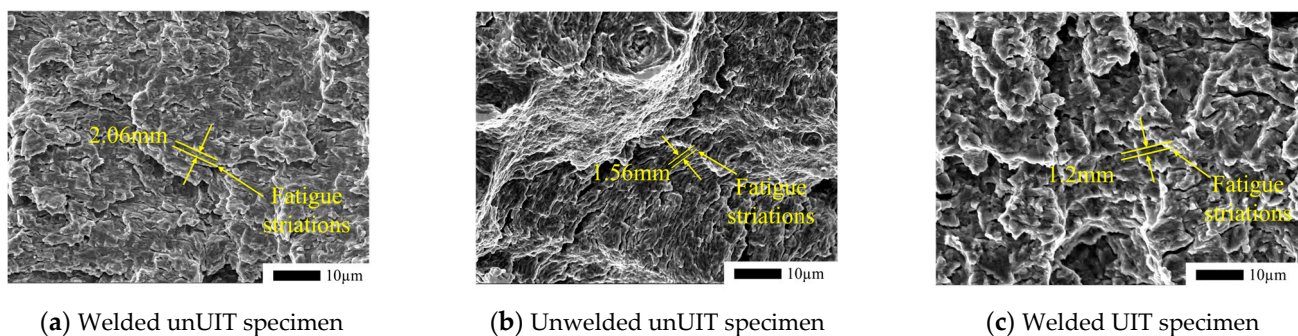


Figure 21. Fracture morphology of the fatigue-crack-extension zone for different specimens.

Figure 22 displays the fracture morphologies corresponding to the crack-propagation and transient fracture regions. All specimens demonstrate a typical ductile fracture mechanism in the transient fracture zone, which is featured by numerous dimples. As can be seen from Figure 22a,b, the dimples of the unUIT specimens are relatively uniform, and the dimple size of the unwelded specimen is larger than that of the welded specimen. As illustrated in Figure 22c, the dimples on the fracture surface are greatly increased in both size and depth after UIT. Meanwhile, obvious tearing edges can be found at the boundaries of the dimples, which proves that large plastic deformation takes place during the final fracture process. Under the same loading conditions, larger dimples generally represent better material toughness. The enlargement and deepening of dimples suggest that the fracture toughness of the material is effectively improved [48–50]. Therefore, it can be

concluded that the toughness of QSTE700TM low-alloy steel is significantly enhanced after ultrasonic impact treatment.

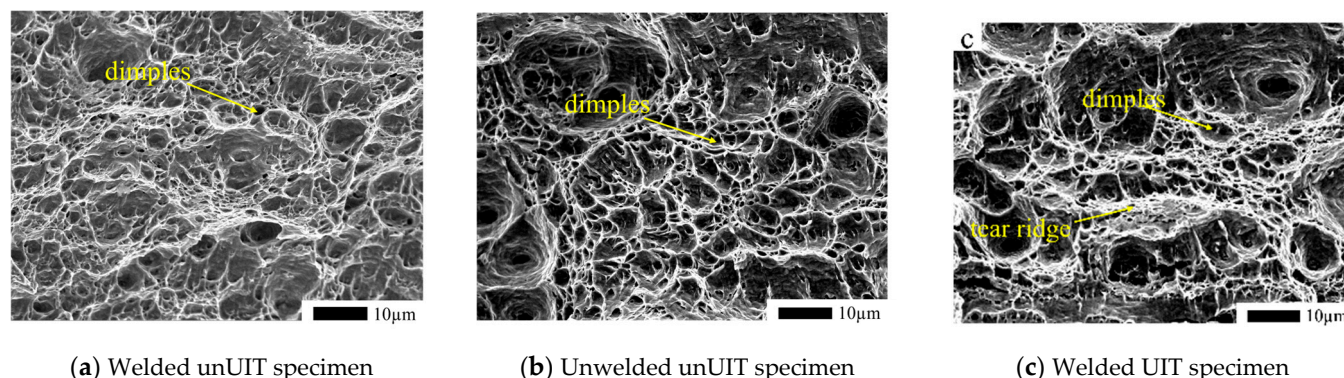


Figure 22. Fracture morphology of the crack-extension transient fracture zone for different specimens.

5. Conclusions

In this paper, the fatigue strength of welded joints of QSTE700TM low-alloy high-strength steel was improved by UIT. A numerical simulation method for the whole process of Welding UIT Fatigue life was established, and the fatigue-fracture morphology was observed. The following conclusions are drawn:

1. ABAQUS finite element software was used to simulate the welding process and the subsequent UIT process for multi-physical field coupling numerical simulation, and the distribution of residual stress in the welded joint before and after UIT was obtained. In the welding state, the simulation data of weld zone and heat-affected zone are highly consistent with the test data, which strongly verifies the reliability and accuracy of the welding simulation. The residual stress after UIT is generally consistent with the test results.
2. Based on the cumulative damage theory, the fatigue life of QSTE700TM low-alloy high-strength steel sheet was predicted by FE-SAFE. The fatigue limit of welded specimens after ultrasonic impact increased by 31.3%, and that of unwelded specimens after ultrasonic shock increased by 42.9%. After UIT, the fatigue life of the specimens increased by 37.1% on average. The difference between experimental results and numerical simulation results is small, and the damage location of numerical simulation and experimental results is basically the same.
3. The fatigue-fracture morphology was observed. After UIT, the crack source of the sample moved to the interior of the material. The crack propagation path becomes tortuous and the crack propagation direction shifts. The dimple size in the fatigue transient fracture zone increases with the decrease in the spacing and the increase in the density of fatigue stripes. These characteristics show that ultrasonic impact can effectively reduce the crack growth rate, improve the fatigue performance and prolong the fatigue life. It is consistent with the fatigue-test results.

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Abbreviations

The following abbreviations are used in this manuscript.

UIT	Ultrasonic impact treatment
FEM	Finite Element Model
JC	Johnson–Cook

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