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Cyclic Void Growth Model Parameter Calibration of Q460D Steel and ER55-G Welds after Exposure to High Temperatures

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Abstract: When high-strength steel is heated to high temperatures and then cooled naturally, its ductility decreases. In earthquake-prone areas, it is necessary to evaluate the ultra-low cycle fatigue fracture (ULCF) behavior of high-strength steel structures after a fire if these structures are used continuously. However, the ULCF fracture model of high-strength steel subjected to high temperatures followed by natural cooling has not been deeply studied. In view of this, twelve notched, round bar specimens fabricated from Q460D steel and ER55-G welds were heated to 900 °C followed by natural cooling and then cyclic loading experiments and finite element analyses (FEA) were performed on these specimens. The fracture deformation obtained from the experiments was used in the FEA to calibrate the damage degradation parameter of a Cyclic Void Growth Model (CVGM) of Q460D steel and ER55-G welds under this condition. The calibrated values were 0.30 and 0.20, respectively. The calibrated CVGM was employed to predict the number of cycles and the force and displacement at the fracture moment of the notched round bar specimens. The predicted results aligned closely with the experimental results, indicating that CVGM is effective in predicting the fracture of Q460D steel and ER55-G welds following exposure to 900 °C and subsequent natural cooling.

Keywords: Q460D high-strength steel; high-temperature heating; natural cooling; ultra-low cycle fatigue fracture; cyclic void growth model



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1. Introduction

With the increase in the height and span of modern buildings, the application of high-strength steel has become more and more popular. The yield strength of high-strength steel has been significantly increased compared with that of conventional low-carbon steel, and the Young's modulus is almost unchanged [1–3]. Fire disasters seriously threaten human life and property safety. Generally, a local fire will not cause a high-strength steel structure building to collapse directly. Due to timely fire extinguishing, high-strength steel structures often do not show obvious residual deformation after a fire, so the high-strength steel structure will not be dismantled, but, at this time, the fracture strain of the high-strength steel is greatly decreased [4]. High-strength steel structures after a fire are more prone to fracture under earthquake action. Therefore, it is necessary to conduct in-depth research on whether high-strength steel structures in earthquake-prone areas can withstand earthquake disasters after a fire. Scholars have begun to investigate the material properties of high-strength steel at elevated temperatures or after exposure to high temperatures [5–12].

Earthquake-induced fatigue is always characterized by large-strain, low-cycle conditions. Cyclic loading due to earthquakes typically involves less than ten cycles and strains that exceed yield strain significantly. This loading style, termed ultra-low cycle fatigue (ULCF) loading, tends to result in ductile fracture as the primary failure mode. The traditional fracture mechanics [13] methods are not applicable for the prediction of ductile fracture with significant yield regions and no initial defect under ULCF loading, while the

micromechanical fracture models show superiority in this regard [14]. The micromechanics-based fracture prediction models suitable for the fracture prediction of structural steels under monotonic loading mainly include the Void Growth Model (VGM) [15] and Stress-Modified Critical Strain (SMCS) model [16], while those suitable for the fracture prediction of structural steels under ULCF loading mainly include the Degraded Significant Plastic Strain (DSPS) model and Cyclic Void Growth Model (CVGM) [17]. Kanvinde and Deierlein [14] carried out coupon tests, smooth notched bar tensile tests, and finite element analysis (FEA) on AW50, AP50, AP110, AP70HP, JW50, JP50, and JP50HP and successfully predicted the fracture of blunt notched specimens by performing parameter calibration of the VGM and SMCS model. Kanvinde and Deierlein [18,19] performed tensile plate tests and FEA of bolted and dog bone connections. The SMCS model and VGM were validated to accurately predict the initiation of ductile fracture in steel connections when subjected to monotonic tensile loading. Roufegarinejad and Tremblay [20] tested five braces under inelastic quasi-static cyclic loading. The experimental results show that the CVGM can predict the fracture initiation moment and fracture location with good accuracy, and the CVGM can be used for the fracture prediction of steel braces with different dimensions. Siriwardane and Ratnayake [21] used a simplified CVGM to obtain satisfactory prediction results for test components. Wang Yuanqing et al. [22] carried out the uniaxial tensile tests on seven local connections that represent beam–column connections in steel frames. They employed the *J*-integral method, VGM, and SMCS model to predict the fracture. The results showed that the fracture prediction using the VGM and SMCS model were more accurate. Liao et al. [23–25] conducted uniaxial tensile experiments and FEA on notched round bar specimens and scanning electron microscope experiments to calibrate the parameters of the SMCS model and VGM for structural steels and their corresponding welds and heat-affected zones. Parameters in the DSPS model and CVGM for these materials were calibrated through cyclic experiments and FEA of notch round bar specimens. The fracture of Q345 steel and Q460 steel welded connections under cyclic loading was successfully predicted by using CVGM [26,27]. Ref. [4] examined the parameter calibrations of VGM of Q460D high-strength steel and ER55-G welds under uniaxial tensile loading after exposure to high temperatures.

Scholars have conducted several relevant experimental and numerical studies on the calibration of micromechanics-based fracture prediction models of structural steels at room temperature [14–27] and the VGM of Q460D high-strength steel and ER55-G welds under uniaxial tensile loading after exposure to high temperatures [4]. However, the fracture model of high-strength steel under ULCF following exposure to high temperatures and subsequent natural cooling has not been deeply studied. There is a relative lack of studies on the application of micromechanical models in the prediction of ULCF fracture of high-strength steel after fires. The yield strength and ultimate strength of a steel coupon specimen heated up to 550 °C and then cooled naturally are usually lower than those of a corresponding steel coupon specimen at room temperature. The yield strength and ultimate strength of a steel coupon specimen heated up to 900 °C and then cooled naturally are usually lower than those of a corresponding steel coupon specimen heated up to 550 °C and then cooled naturally. In an actual fire, the temperature of a building may arrive at 900 °C in a relatively short time. The purpose of this research was to study the fracture performance of Q460D high-strength steel and ER55-G welds after a fire. In view of this, the damage degradation parameter of the CVGM of Q460D high-strength steel and ER55-G welds under ULCF after exposure to 900 °C followed by natural cooling was calibrated through tests and FEA, and the suitability of the calibrated CVGM in predicting the ULCF fracture initiation of these materials under this condition was verified in this study. The findings of this study offer insights into evaluating the ULCF fracture behavior of Q460 high-strength steel structures after a fire and contribute to the assessment of the safety of actual Q460D high-strength steel structures after a fire. The methodology of this study is illustrated in Figure 1.

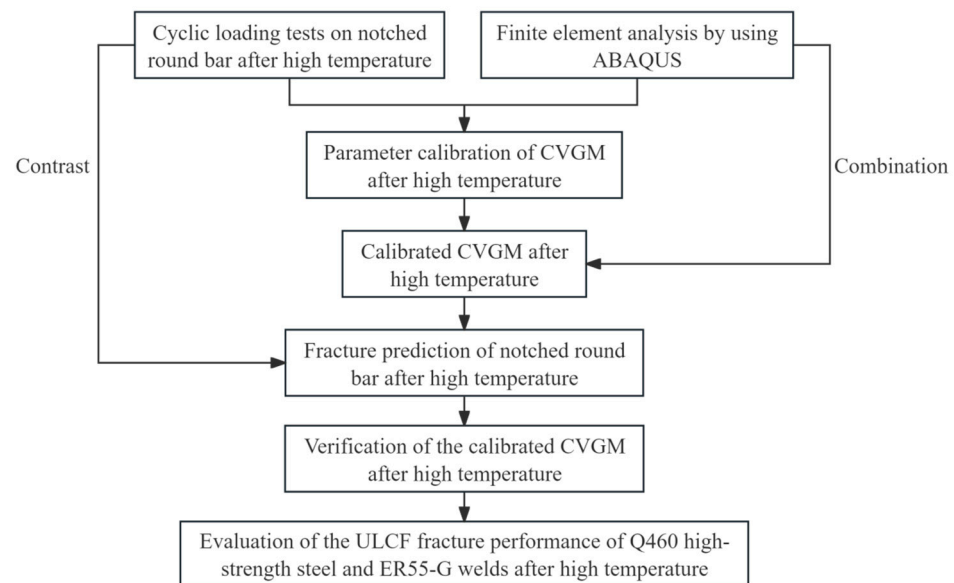


Figure 1. Methodology of this study.

2. Basic Theory of Micromechanical Fracture Model CVGM

In 2004, Kanvinde and Deierlein [17] highlighted that the conditions of void growth under tensile cycles differ significantly from those under compressive cycles when subjected to cyclic loading, suggesting that these conditions need to be addressed separately, and then further expanded the VGM and proposed the CVGM. The CVGM fracture criterion is expressed in Equation (1).

$$\exp(-\lambda_{\text{CVGM}}\varepsilon_p) \cdot \eta = \sum_{\varepsilon_1}^{\varepsilon_2} \exp(|1.5T|) \cdot d\varepsilon_t - \sum_{\varepsilon_1}^{\varepsilon_2} \exp(|1.5T|)d\varepsilon_c \quad (1)$$

Here, λ_{CVGM} represents the damage degradation parameter of the CVGM, η represents the fracture toughness parameter of the VGM, ε_1 denotes the strain at a loading cycle's start, ε_2 denotes the strain at a loading cycle's end, ε_t is the strain of the material under the tensile cycle, ε_c is the strain of the material under the compressive cycle, and ε_p denotes equivalent plastic strain. The value on the left-hand side of Equation (1) is represented by η_{cyclic} , as shown in Equation (2), and the value of η_{cyclic} reflects the cyclic void expansion ability of the material. The value on the right-hand side of Equation (1) is represented by VGD_{cyclic} , as shown in Equation (3), and the value of VGD_{cyclic} reflects the damage accumulation under cyclic loading. When the value of η_{cyclic} equals that of VGD_{cyclic} , fracture can be predicted to occur.

$$\eta_{\text{cyclic}} = \exp(-\lambda_{\text{CVGM}}\varepsilon_p) \cdot \eta \quad (2)$$

$$VGD_{\text{cyclic}} = \sum_{\varepsilon_1}^{\varepsilon_2} \exp(|1.5T|) \cdot d\varepsilon_t - \sum_{\varepsilon_1}^{\varepsilon_2} \exp(|1.5T|)d\varepsilon_c \quad (3)$$

In the CVGM, the ratio of $\eta_{\text{cyclic}}/\eta$ can be directly used as the damage ratio, and the equivalent plastic strain value at the start of the final tensile cycle ε_p is taken as the damage variable D . The functional relation Equation (4) of D and $\eta_{\text{cyclic}}/\eta$ can be obtained by fitting them with the least square method; then, the λ_{CVGM} of the CVGM can be obtained.

$$f(D) = \frac{\eta_{\text{cyclic}}}{\eta} = \exp(-\lambda_{\text{CVGM}} \cdot D) \quad (4)$$

In CVGM, the initiation of ductile fracture does not pertain to an isolated material point but rather involves a critical volume of material. So, characteristic length parameter l^* that involves several material points needs to be defined. The initiation of ductile fracture can be predicted when the CVGM fracture criterion is satisfied over the characteristic length

l^* . The value of l^* is dependent on the microstructure of the material and can be derived from examining images taken with a scanning electron microscope. Two bounds and the most possible value of characteristic length parameter l^* were proposed in ref. [17]. The lower bound was designated as twice the mean dimple diameter because fracture can be defined as the coalescence of two adjacent voids, and previous studies have also shown that twice the dimple size is reasonable and could be used as a conservative value of l^* . The upper bound was designated as the length of the largest plateau or trough identified in the scanning electron microscope tests. The mean value of l^* is obtained by computing the mean of ten measurements of the lengths of plateaus and troughs. It represents the most possible approximation of the value of l^* . In the case of a high-stress–strain gradient, the value of l^* has a great effect on fracture prediction results.

It has been proven that the CVGM is suitable for predicting the initiation of ductile fracture in Q460 steel welded connections subjected to ULCF loading [27]. However, there is a relative lack of research on whether the CVGM could be employed to predict the initiation of ductile fracture in Q460D steel and ER55-G welds following exposure to a high temperature of 900 °C followed by natural cooling under ULCF loading. This study aimed to verify the applicability of the CVGM under this condition.

3. Cyclic Experiments on Notched Round Bar Specimens Following Exposure to High Temperatures Followed by Natural Cooling

3.1. Coupon Test Results

In ref. [4], smooth round bar specimens fabricated from Q460D high-strength steel and ER55-G welds were heated within the high-temperature device shown in Figure 2. The heating temperature of the specimens was systematically raised at a rate of approximately 10 °C per minute until the desired temperature of 900 °C was reached, which took roughly 90 min. Acknowledging that prolonged exposure to a constant temperature has a negligible impact on the properties of the steel [28], the specimens were maintained at 900 °C for a duration of 30 min. Subsequently, the specimens were taken out of the high-temperature electric furnace and directly placed in indoor air for cooling to room temperature. Uniaxial tensile experiments were performed on these specimens. The Q460D steel and ER55-G welds in ref. [4] belonged to the same batches as the materials studied in this paper.



Figure 2. High-temperature test setup [4]. (a) High-temperature electric furnace. (b) Electric furnace temperature controller.

Table 1 presents the yield strength σ_y , ultimate strength σ_u , and elastic modulus E of the Q460D steel and ER55-G welds at room temperature and following exposure to 900 °C with subsequent natural cooling. Figure 3 illustrates the true stress–plastic strain curve of the coupon test specimen from the beginning of the test to the fracture moment. It can be concluded from Figure 3 that the true stresses of the Q460D steel at the yield moment and the fracture moment were higher while the plastic strain of Q460D steel was

lower compared to those of ER55-G welds. The key data points derived from the true stress–plastic strain curve of each material are summarized in Table 2.

Table 1. Mechanical properties of coupon test specimen.

Material	Specimen Number	Yield Strength σ_y (MPa)	Ultimate Strength σ_u (MPa)	Elastic Modulus E (MPa)
Q460D steel (room temperature)	QD-1	419	540	200,703
	QD-2	478	618	200,191
	QD-3	460	612	202,848
	Mean value	452	590	201,247
Q460D steel (following exposure to 900 °C and subsequent natural cooling)	QZD-1	351	539	183,341
	QZD-2	352	545	181,774
	QZD-3	352	538	178,606
	Mean value	352	541	181,240
ER55-G welds (room temperature)	ED-1	516	621	209,104
	ED-2	498	616	204,415
	ED-3	509	606	201,321
	Mean value	508	614	204,947
ER55-G welds (following exposure to 900 °C and subsequent natural cooling)	EZD-1	275	511	198,462
	EZD-2	281	487	199,919
	EZD-3	273	487	185,750
	Mean value	276	495	194,710

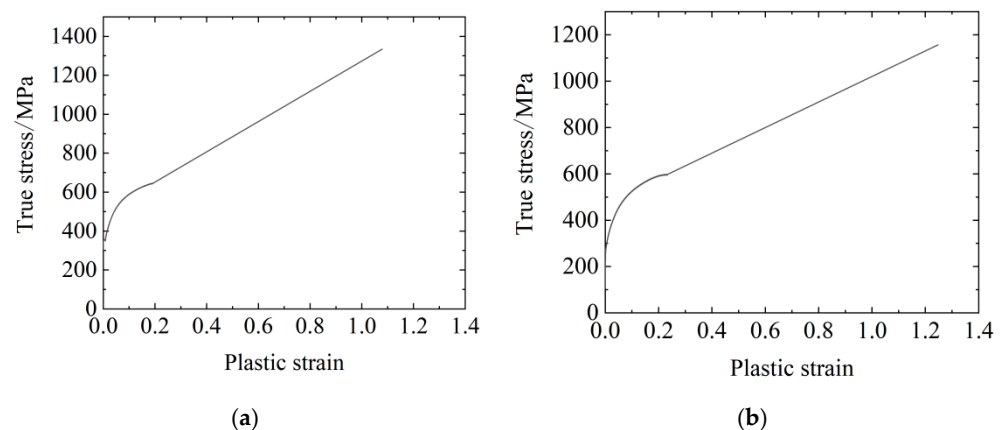


Figure 3. True stress–plastic strain curves of Q460D steel and ER55-G welds following exposure to 900 °C and subsequent natural cooling. (a) Q460D steel. (b) ER55-G welds.

Table 2. The key data points from the true stress–plastic strain curve.

Q460D steel (following exposure to 900 °C and subsequent natural cooling)	Plastic strain	0.000	0.0001	0.0005	0.001	0.004	0.008	0.016	0.032
	True stress (MPa)	302	343	350	351	354	355	404	471
	Plastic strain	0.049	0.065	0.081	0.099	0.131	0.164	0.192	1.079
	True stress (MPa)	516	546	566	588	613	633	645	1334
ER55-G welds (following exposure to 900 °C and subsequent natural cooling)	Plastic strain	0.000	0.0001	0.0005	0.001	0.0014	0.004	0.008	0.018
	True stress (MPa)	107	166	228	255	264	289	315	361
	Plastic strain	0.036	0.049	0.068	0.088	0.131	0.179	0.233	1.248
	True stress (MPa)	419	452	484	511	551	582	598	1157

3.2. Design and Manufacture of Notched Round BAR Cyclic Test Specimens

As shown in Figure 4, notched round bar specimens with a gauge length part of Q460D steel were extracted from a 30mm thick hot-rolled steel plate that belonged to the same batch of material as the Q460D steel introduced in Section 3.1. As shown in Figure 5,

two Q460D steel plates with thicknesses of 30 mm that belonged to the same batch of material as the steel introduced in Section 3.1 were welded with ER55-G butt welds that belonged to the same batch of material as the welds introduced in Section 3.1. The welding process was the same as that in ref. [4], which was determined according to ref. [29]. The welding quality reached the first grade. Notched round bar specimens with a gauge length part of ER55-G weld were extracted from the welded steel plate in a direction vertical to the weld length direction, as illustrated in Figure 5. Referring to ref. [17], the design dimensions of the specimens with notch radii of 6.25 mm, 3.125 mm, and 1.5 mm, respectively, are depicted in Figure 6.

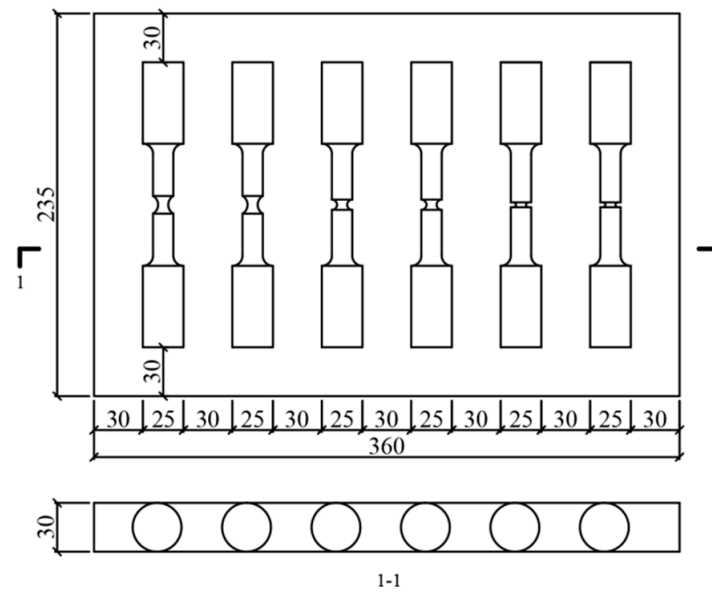


Figure 4. Sampling of Q460D steel specimens (mm).

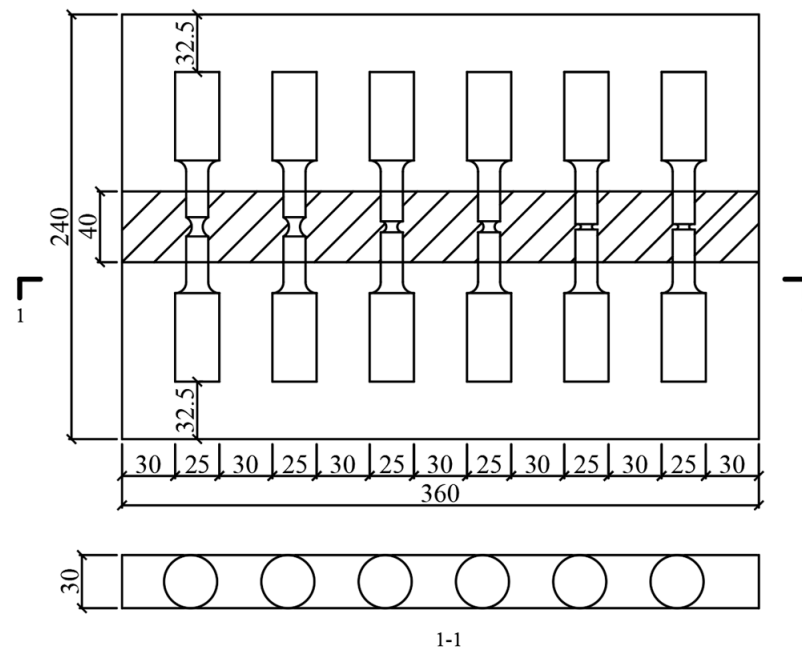


Figure 5. Sampling of ER55-G weld specimens (mm).

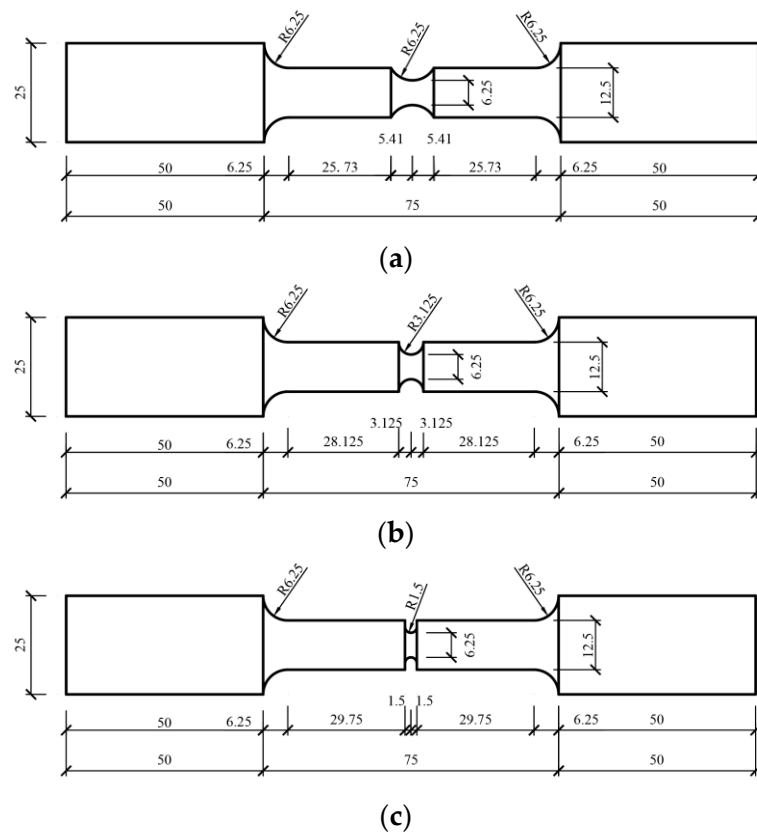


Figure 6. Design dimensions of notched round bar specimen (mm). (a) $R = 6.25$ mm. (b) $R = 3.125$ mm. (c) $R = 1.5$ mm.

3.3. Treatment of Thermal Exposure and Subsequent Natural Cooling of Notched Round Bar Specimens

The notched round bar specimens were put into the high-temperature device shown in Figure 2. The treatments of thermal exposure and subsequent natural cooling of the notched round bar specimens were consistent with those of the coupon test specimens introduced in Section 3.1. Figure 7 presents photos of representative notched round bar specimens following their exposure to elevated temperatures and natural cooling.

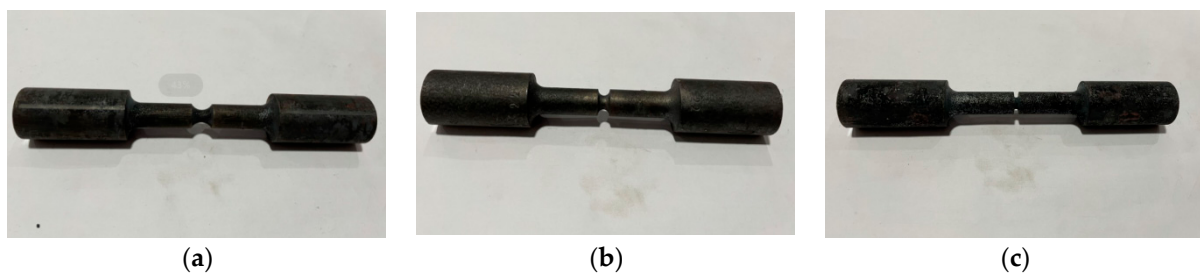


Figure 7. Photos of representative notched round bar specimens following exposure to elevated temperatures and subsequent natural cooling. (a) $R = 6.25$ mm. (b) $R = 3.125$ mm. (c) $R = 1.5$ mm.

3.4. ULCF Loading Test on Notched Round Bar Specimen Following Exposure to High Temperatures and Subsequent Natural Cooling

The ULCF loading test was performed on the cooled notched round bar specimen by using the MTS793 testing machine, as shown in Figure 8. An electronic strain extensometer with a gauge section length of 25 mm was utilized to measure the uniaxial strain of the specimen.

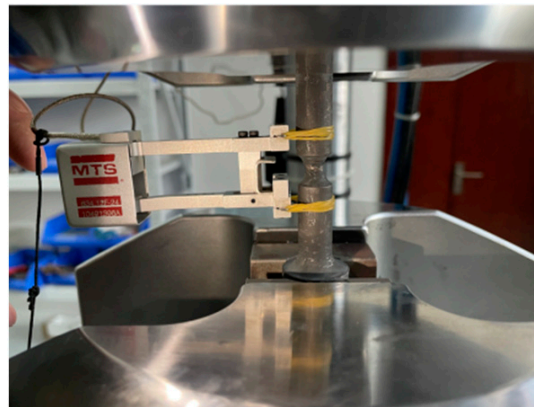


Figure 8. Cyclic test specimens.

The deformation of the specimen's gauge length part was used to control loading. According to ref. [17], two types of loading protocols, CL-1 and CL-2, were adopted, as shown in Figure 9. In Figure 9, Δ_f is the fracture deformation of the gauge length part of the notched round bar specimen under uniaxial tensile load. $1/2\Delta_f$ was adopted as the loading amplitude of CL-1, while $1/4\Delta_f$ was adopted as the loading amplitude of CL-2. During the cyclic test using loading protocol CL-1, the deformation of the specimen's gauge length part was cycled between 0 and the loading amplitude until failure occurred. During the cyclic test using loading protocol CL-2, the deformation of the specimen's gauge length part was cycled between 0 and the loading amplitude for five cycles and then a tensile load was applied to the specimen until failure occurred. Table 3 shows the number, measured dimensions, and loading amplitude of each notched round bar cyclic test specimen.

In Table 3, specimen numbers with Q denote the use of Q460D steel, while those with E indicate ER55-G welds. Additionally, Z signifies a treatment involving heating up to 900 °C followed by natural cooling and W suggests cyclic loading. The last numbers 1, 3, and 5 of the specimen numbers correspond to the loading protocol CL-1 shown in Figure 9a; the last numbers 2, 4, and 6 of the specimen numbers correspond to the loading protocol CL-2 shown in Figure 9b; and (5) indicates five cycles of cyclic loading.

The force–deformation curves of each specimen obtained from the experiment, as depicted in Figures 10 and 11, showed a sudden change in slope at a specific moment during the final tensile cycle, and this was considered as the point corresponding to the initiation of ductile fracture under cyclic loading. It can be observed from Figures 10 and 11 that the maximum compressive force was higher than the maximum tensile force during the cyclic loading. The deformation Δ_f^{cyclic} was recorded for use in the FEA.

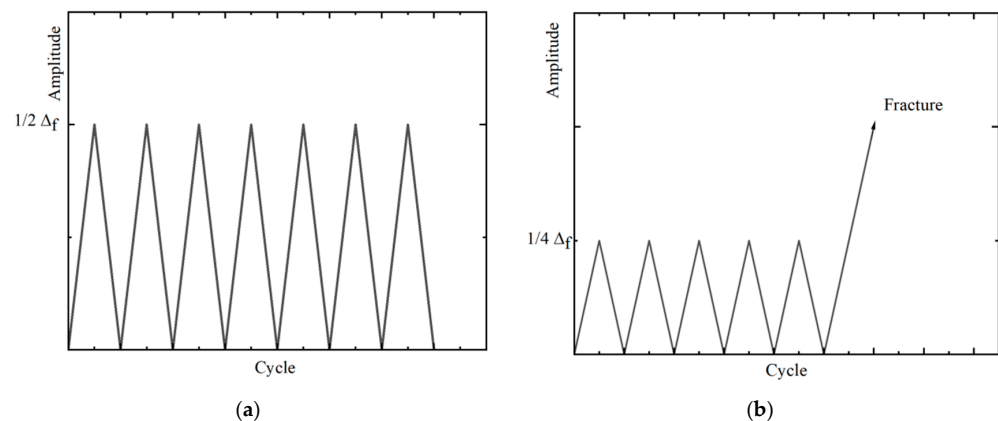
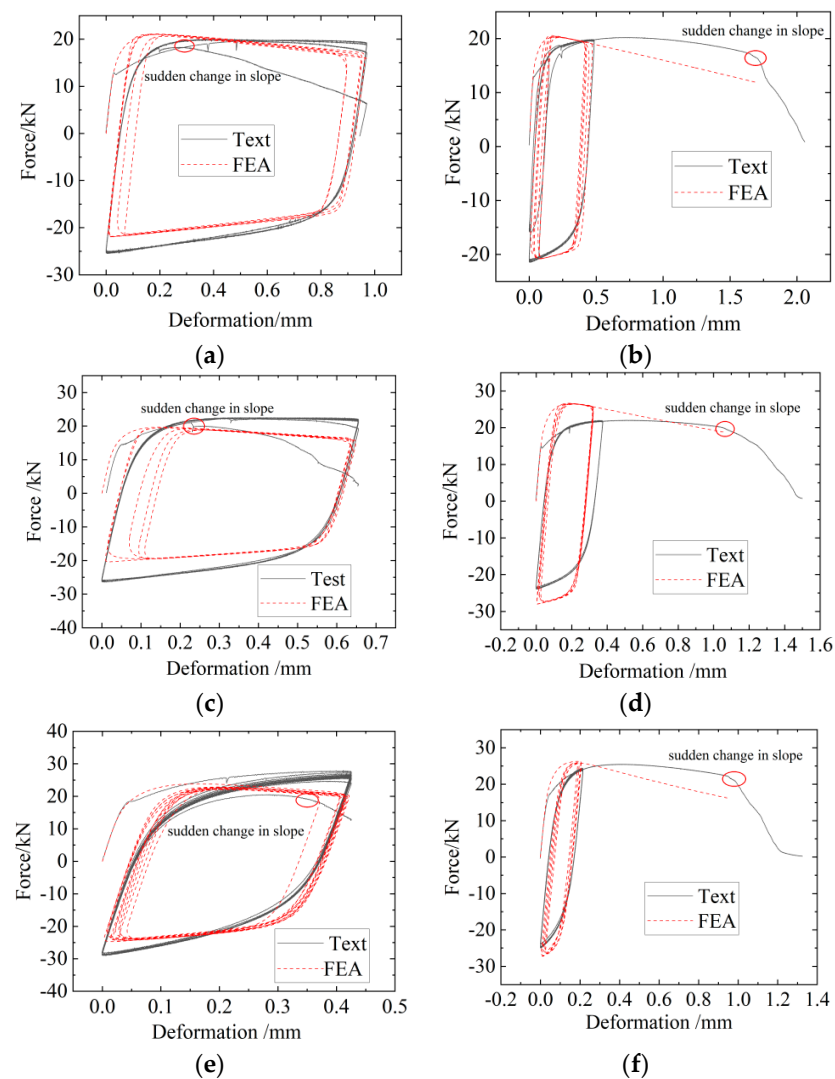


Figure 9. Loading protocol. (a) Loading protocol CL-1. (b) Loading protocol CL-2.

Table 3. The number, measured dimensions, and loading amplitude of each specimen.

Material	Specimen Number	Notch Radius	Diameter of Gauge Length Part	Length of Clamping Part	Diameter at the Notch	Loading Amplitude
		R (mm)	(mm)	(mm)	d_0 (mm)	(mm)
Q460D steel	QZ-W-1	6.25	12.80	50.23	6.88	0.97
	QZ-W-2		12.80	50.22	6.76	0.485 (5)
	QZ-W-3	3.125	12.86	50.26	6.32	0.655
	QZ-W-4		12.70	50.23	7.28	0.3275 (5)
	QZ-W-5	1.5	12.64	50.19	6.48	0.425
	QZ-W-6		12.76	50.29	6.76	0.2125 (5)
ER55-G welds	EZ-W-1	6.25	12.62	49.22	6.38	1.385
	EZ-W-2		12.60	49.27	6.38	0.6925 (5)
	EZ-W-3	3.125	12.58	49.15	6.38	0.915
	EZ-W-4		12.58	49.32	6.38	0.4575 (5)
	EZ-W-5	1.5	12.68	49.54	6.34	0.745
	EZ-W-6		12.58	49.58	6.36	0.3725 (5)

**Figure 10.** Comparison of FEA with test results for Q460D steel specimens following exposure to 900 °C and subsequent natural cooling. (a) Specimen QZ–W–1. (b) Specimen QZ–W–2. (c) Specimen QZ–W–3. (d) Specimen QZ–W–4. (e) Specimen QZ–W–5. (f) Specimen QZ–W–6.

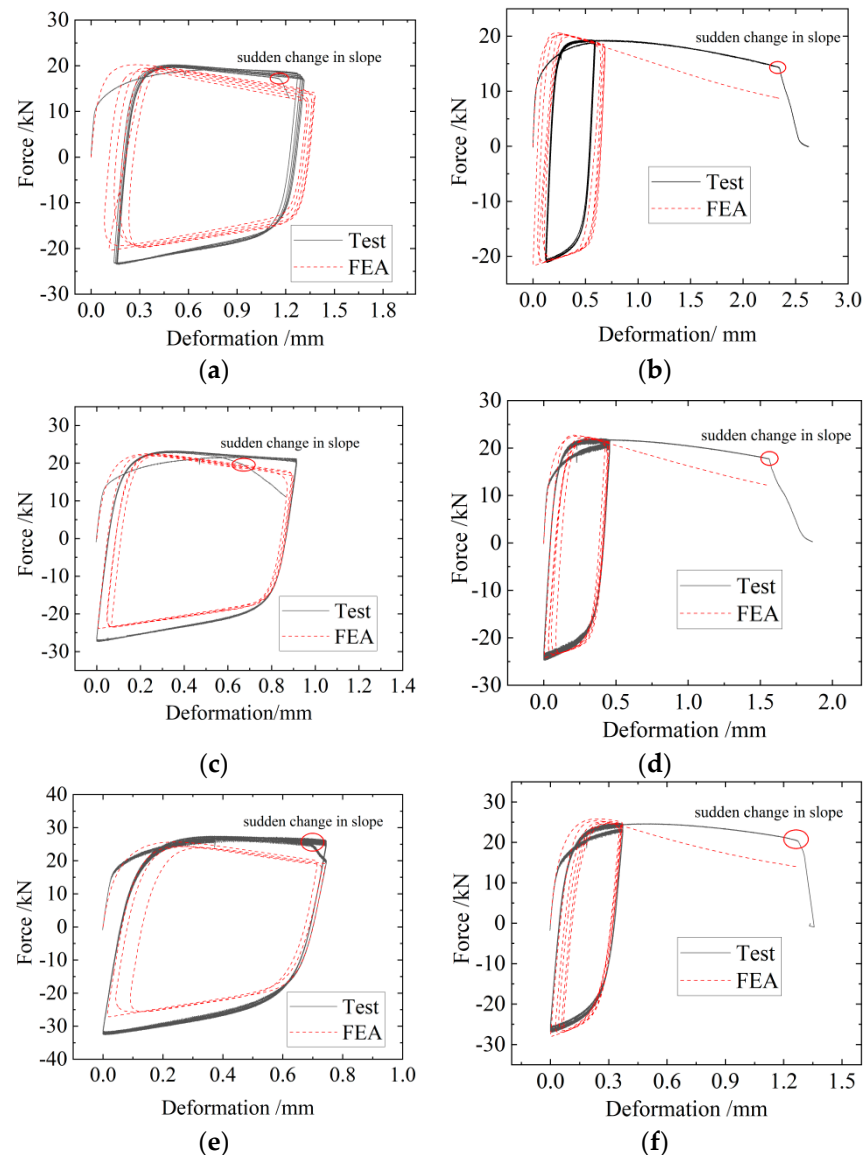


Figure 11. Comparison of FEA with test results for ER55-G weld specimens following exposure to 900 °C and subsequent natural cooling. (a) Specimen EZ-W-1. (b) Specimen EZ-W-2. (c) Specimen EZ-W-3. (d) Specimen EZ-W-4. (e) Specimen EZ-W-5. (f) Specimen EZ-W-6.

4. FEA of Notched Round Bar Specimen Following Exposure to High Temperatures and Subsequent Natural Cooling

FEA using ABAQUS 6.14 was performed on the twelve notched, round bar specimens. Nonlinear, large deformation behavior, and cyclic plasticity were employed. Based on the Lemaitre–Chaboche model [30], the cyclic plasticity model adopted a von Mises yield surface combining nonlinear isotropic and kinematic hardening. The parameters in the kinematic hardening component of the model were substituted in the ABAQUS model by an array of true stresses and plastic strains, shown in Table 2. Equation (5) describes the isotropic hardening component.

$$\sigma^0 = \sigma|_0 + Q_\infty \left(1 - e^{-b \cdot \varepsilon_p}\right) \quad (5)$$

Here, the current elastic range σ^0 is defined as a function of the initial elastic range $\sigma|_0$, ε_p is the equivalent plastic strain, and Q_∞ and b are two parameters. Isotropic hardening was determined by entering the values of Q_∞ and b in the “CYCLIC HARDENING” option in ABAQUS. The values of Q_∞ and b were determined by a trial process to achieve the best

agreements between the notched round bar force–deformation curves obtained from the experiments and the FEA, as shown in Table 4. A two-dimensional, axisymmetric FEA of the specimen's gauge length part with an element type of CAX8R was conducted, as shown in Figure 12. The top and bottom sections of the finite element model were specified as rigid and tied to the reference points at the center of the top and bottom sections, respectively. The bottom reference point was completely fixed. The constraints on degrees of freedom U1, U3, and UR2 along the symmetric axis were enforced, allowing only movement in the U2 direction. The top section was subjected to a load causing displacement solely along the U2 direction. The initiation of ductile fracture could be predicted when the CVGM was satisfied over the characteristic length l^* . The influence of cyclic loading on the value of l^* was difficult to determine, and the value of l^* was taken to be the same as that under monotonic loading [17]. According to ref. [4], the mean values of l^* of Q460D steel and ER55-G welds following exposure to 900 °C and subsequent natural cooling are 0.162 mm and 0.124 mm, respectively. The finite element model utilized these measurements as the element sizes at the critical notched region of the specimen. For areas deemed non-critical, larger element sizes were used to enhance the computational efficiency, as depicted in Figure 13.

As can be found from Figure 14, the stress–strain gradient in the notch area of the specimen subjected to ULCF loading was very low, and the stress–strain fields in that area could satisfy the CVGM fracture criterion almost simultaneously, which minimized the failure dependence on the characteristic length l^* ; therefore, the cyclic tests and FEA of notched round bar specimens could be adopted to calibrate the damage degradation parameter of the CVGM.

Table 4. Cyclic hardening model parameters for each material.

Material	Equivalent Stress (MPa)	Q_∞	b
Q460D steel	302	180	145
ER55-G welds	107	205	80

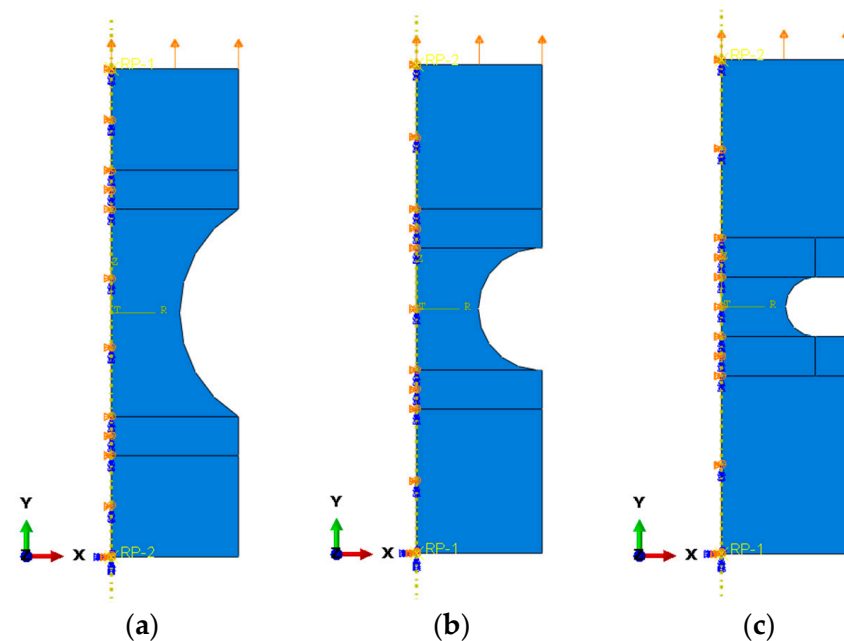


Figure 12. FEA model of notched round bar specimens. (a) $R = 6.25$ mm. (b) $R = 3.125$ mm. (c) $R = 1.5$ mm.

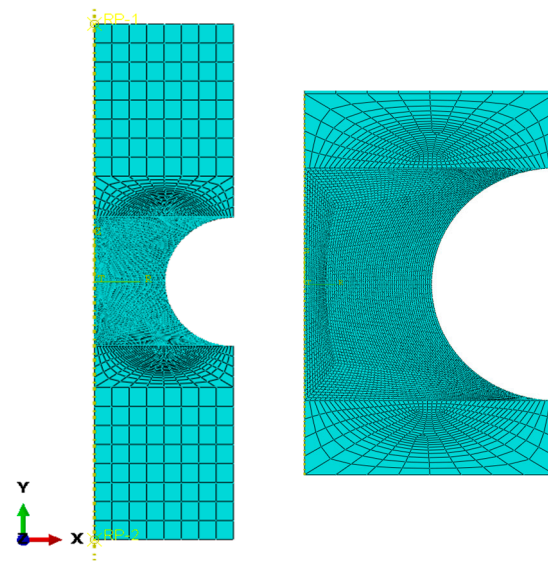


Figure 13. The meshing of the FEA model of Specimen EZ-W-3.

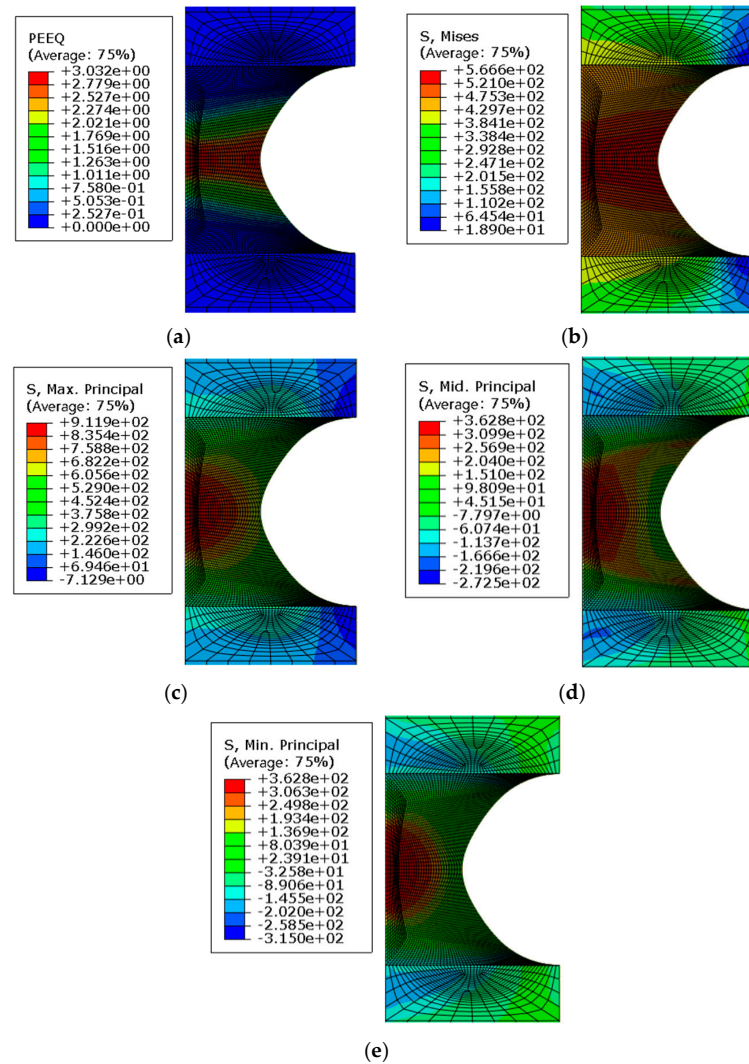


Figure 14. Strain and stress contours in the notch area of Specimen EZ-W-3. (a) Equivalent plastic strain. (b) von Mises stress. (c) Maximum principal stress. (d) Middle principal stress. (e) Minimum principal stress.

The force–deformation curves from the FEA and the tests, as illustrated in Figures 10 and 11, displayed a good agreement for most of the specimens, while the deviation of the FEA curve with the test curve of specimen QZ-W-4 was relatively large. The reason for this may be that the parameters Q_∞ and b used in the FEA were obtained from a trial process, and occasionality always occurs in this test.

5. Calibration of CVGM Parameters

Through FEA of each notched round bar specimen subjected to cyclic loading, it was possible to extract stress and strain from the central element at the notch of each specimen, and the damage accumulation VGD_{cyclic} of each specimen under cyclic loading could be obtained by bringing the stress and strain into Equation (3).

When fracture initiation occurred, the value of η_{cyclic} equaled that of VGD_{cyclic} . According to ref. [4], the values of parameter η of the VGM of Q460D steel and ER55-G welds following exposure to 900 °C and subsequent natural cooling were 1.52 and 2.74, respectively. The ratio of η_{cyclic} at the failure point to η was used to estimate the damage ratio of each specimen and the equivalent plastic strain at the start of the final tensile cycle of the specimen was designated as the damage variable D . A graphical representation was created to display the relationship between the damage ratio $\eta_{\text{cyclic}}/\eta$ and the damage variable D . This process was repeated for all the notched round bar cyclic tests of Q460D steel and ER55-G welds, respectively. The results of these analyses are summarized in scatter plots illustrated in Figures 15 and 16, respectively. Utilizing a least squares fit, the damage ratio was determined as a function of the damage variable. Equation (4) represents an exponential function adopted for the damage function. Figures 15 and 16 display the exponential curve fitted to the scatter data. This method facilitated the determination of the damage degradation parameter, denoted as λ_{CVGM} , for each material. Following exposure to 900 °C and subsequent natural cooling, the values of λ_{CVGM} were found to be 0.30 for Q460D steel and 0.20 for ER55-G welds.

As can be seen from Figures 15 and 16, when calibrating the parameter λ_{CVGM} of Q460D steel and ER55-G welds following exposure to 900 °C and subsequent natural cooling, most of the points were near the fitted curve, but some points were still relatively far from the fitted curve, indicating that the calibrated values of parameter λ_{CVGM} were relatively reliable within a certain deviation range. The deviation of the exponential relationship between the damage ratio and the damage variable in Equation (4) from the test data may have been due to the mean values of η used in the calibration process [4]. For the CVGM, the deviation of η may have caused the deviation between the exponential relationship and the test data. The test occasionality may have been another reason for the deviation.

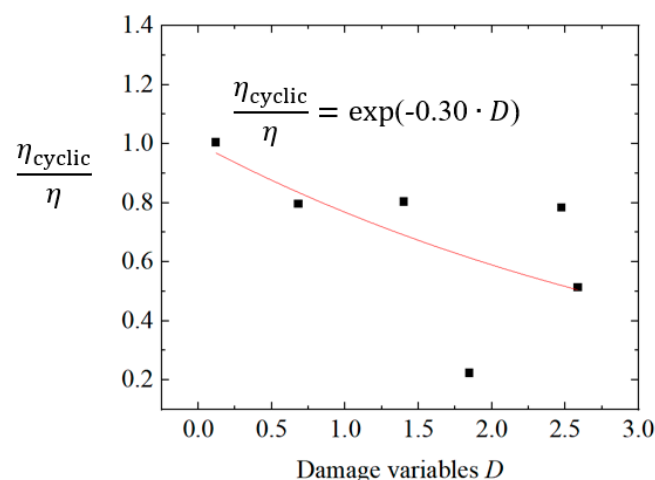


Figure 15. Scatter plot and curve fit for calibrating λ_{CVGM} of Q460D steel following exposure to 900 °C and subsequent natural cooling.

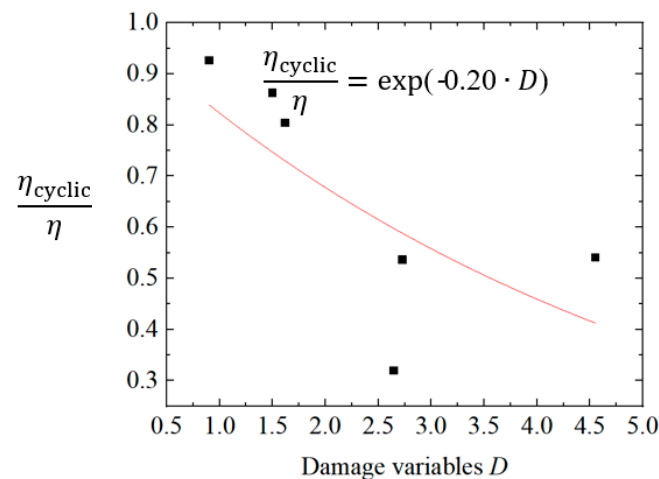


Figure 16. Scatter plot and curve fit for calibrating λ_{CVGM} of ER55-G welds following exposure to 900 °C and subsequent natural cooling.

6. Fracture Prediction

6.1. CVGM Prediction Principle

Based on the FEA calculation results, von Mises stress, principal stresses in three directions, and equivalent plastic strain of the most dangerous element were extracted. According to Equations (2) and (3), the cyclic void expansion capacity at each tensile cycle's start η_{cyclic} and the damage accumulation VGD_{cyclic} under tensile–compressive cyclic loading were calculated, respectively, and when the difference between VGD_{cyclic} and η_{cyclic} was greater than 0, it could be predicted that fracture initiation occurred. Figures 17 and 18 show the relationship of the fracture indicators including VGD_{cyclic} and η_{cyclic} to the number of cycles. In Figures 17 and 18, the fracture indicator η_{cyclic} decreases with the increase in the number of cycles. When the curve representing VGD_{cyclic} intersected with the curve representing η_{cyclic} , fracture could be predicted to occur.

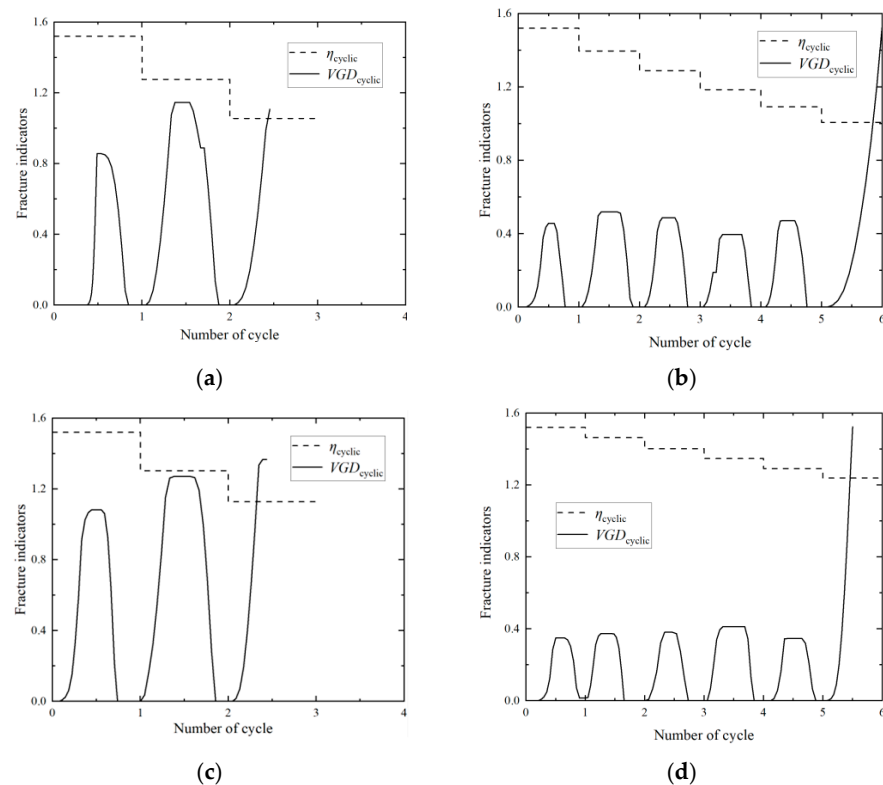


Figure 17. Cont.

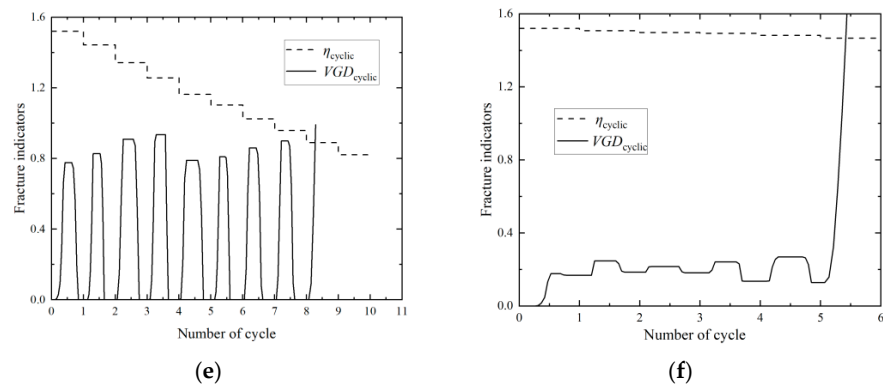


Figure 17. Fracture prediction of Q460 steel using CVGM. (a) Specimen QZ-W-1. (b) Specimen QZ-W-2. (c) Specimen QZ-W-3. (d) Specimen QZ-W-4. (e) Specimen QZ-W-5. (f) Specimen QZ-W-6.

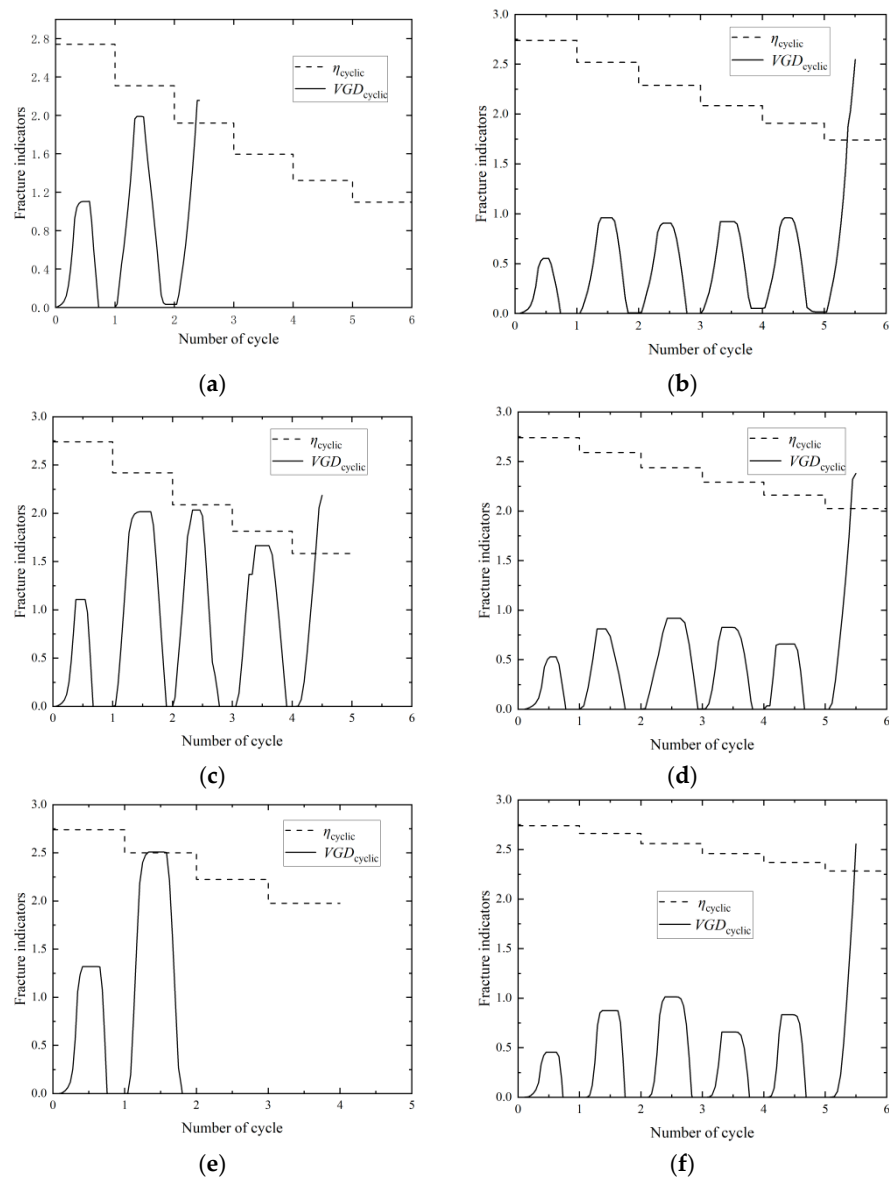


Figure 18. Fracture prediction of ER55-G welds using CVGM. (a) Specimen EZ-W-1. (b) Specimen EZ-W-2. (c) Specimen EZ-W-3. (d) Specimen EZ-W-4. (e) Specimen EZ-W-5. (f) Specimen EZ-W-6.

6.2. CVGM Prediction Results

A comparison of the fracture prediction results of the CVGM with the experimental results is shown in Table 5. As can be found from Table 5, the predicted fracture load, fracture displacement, and number of cycles to fracture of most of the notched round bar specimens agreed well with the experimental results. The deviations of the predicted results of specimens QZ-W-3, QZ-W-5, and EZ-W-1 were relatively large. It can be found from Figures 17 and 18 that the points determined by the damage ratio $\eta_{\text{cyclic}}/\eta$ and the damage variable D of these specimens were relatively far from the fitted curve. In addition, the mean values of η for Q460D steel and ER55-G welds following exposure to 900 °C and subsequent natural cooling, as mentioned in ref. [4], were used in the calibration process of λ_{CVGM} . There were deviations in the values of η . Furthermore, there may have been test occasionality. These may be the reasons for the relatively large deviations. It was indicated that the calibrated CVGM could be employed to predict the initiation of ductile fracture in Q460D steel and ER55-G welds following exposure to 900 °C and subsequent natural cooling under ULCF loading.

Table 5. Comparison of CVGM fracture prediction results with experimental results.

Specimen Number		Fracture Load (kN)	Fracture Displacement (mm)	Number of Cycles
QZ-W-1	Test	18.13	0.30	4
	Prediction	23.49	0.86	3
	Deviation	29.56%	186.67%	−1
QZ-W-2	Test	17.02	1.66	6
	Prediction	22.75	1.21	6
	Deviation	33.67%	−27.11%	0
QZ-W-3	Test	20.95	0.23	6
	Prediction	21.61	0.64	3
	Deviation	3.15%	178.26%	−3
QZ-W-4	Test	20.09	1.05	6
	Prediction	29.16	1.06	6
	Deviation	45.15%	0.95%	0
QZ-W-5	Test	18.55	0.36	12
	Prediction	25.30	0.18	8
	Deviation	36.39%	−50%	−4
QZ-W-6	Test	22.07	0.95	6
	Prediction	28.46	0.66	6
	Deviation	28.95%	−30.53%	0
EZ-W-1	Test	17.13	1.16	6
	Prediction	16.81	1.31	3
	Deviation	−1.87%	12.93%	−3
EZ-W-2	Test	14.19	2.35	6
	Prediction	17.28	1.65	6
	Deviation	21.78%	−29.79%	0
EZ-W-3	Test	19.64	0.67	5
	Prediction	17.80	0.57	5
	Deviation	−9.37%	−14.93%	0
EZ-W-4	Test	17.62	1.56	6
	Prediction	18.66	1.51	6
	Deviation	5.90%	−3.21%	0
EZ-W-5	Test	24.53	0.70	4
	Prediction	21.72	0.73	2
	Deviation	−11.46%	4.29%	−2
EZ-W-6	Test	20.38	1.27	6
	Prediction	21.41	0.67	6
	Deviation	5.05%	−47.24%	0

7. Conclusions

The ductility of high-strength steel typically decreases following exposure to high temperatures and subsequent natural cooling. In earthquake-prone areas, the ULCF fracture behavior of high-strength steel under such conditions needs to be evaluated. However, there remains a notable gap in research regarding the fracture models of high-strength steel after undergoing high-temperature treatment followed by natural cooling. Specifically, the utilization of micromechanical models for predicting ULCF fracture in high-strength steel post-high-temperature-exposure has not been thoroughly explored. To address this gap, cyclic loading tests and FEA were performed on twelve notched round bar specimens fabricated from Q460D high-strength steel and ER55-G welds subjected to a high temperature of 900 °C followed by natural cooling. The parameters of the CVGM were calibrated for both Q460D steel and ER55-G welds under these conditions. Subsequently, the calibrated CVGM was employed to predict the initiation of ductile fracture in the twelve notched round bar specimens fabricated from Q460D steel and ER55-G welds post-exposure to 900 °C and natural cooling. From these investigations, the following conclusions were drawn.

- (1) The true stresses of Q460D steel at the yield moment and the fracture moment were higher while the plastic strain of Q460D steel was lower compared with those of ER55-G welds.
- (2) The force–deformation curves from the FEA and the tests displayed good agreement for most of the notched round bar specimens, while the deviation of the FEA curve with the test curve of some notched round bar specimens was relatively large. The reason for this may be that the parameters Q_∞ and b used in the FEA were obtained from a trial process, and occasionality always occurs in this test.
- (3) The damage degradation parameter λ_{CVGM} of the CVGM of Q460D steel and ER55-G welds following exposure to 900 °C and subsequent natural cooling under ULCF loading was 0.30 and 0.20, respectively. The value of the characteristic length parameter l^* of the CVGM of Q460D steel and ER55-G welds post-high-temperature-treatment at 900 °C and natural cooling under ULCF loading was determined to be 0.162 mm and 0.124 mm, respectively.
- (4) The fracture index η_{cyclic} decreased with the increase in the number of cycles.
- (5) The calibrated CVGM was capable of accurately predicting the initiation of ductile fracture in Q460D steel and ER55-G welds following exposure to 900 °C and subsequent natural cooling under ULCF loading. The deviations of the predicted results of specimens QZ-W-3, QZ-W-5, and EZ-W-1 were relatively large. The reasons for this may be that the points determined by the damage ratio η_{cyclic} / η and the damage variable D of these specimens were relatively far from the fitted curve, the mean values of η for Q460D steel and ER55-G welds following exposure to 900 °C and subsequent natural cooling mentioned in ref. [4] were used in the calibration process of λ_{CVGM} , there were deviations of the values of η , and there may have been test occasionality.

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References

1. Bjorhovde, R. Development and use of high performance steel. *J. Constr. Steel Res.* **2004**, *60*, 393–400. [\[CrossRef\]](#)
2. Ke, K.; Chen, Y.; Zhou, X.; Yam, M.C.; Hu, S. Experimental and numerical study of a brace-type hybrid damper with steel slit plates enhanced by friction mechanism. *Thin-Walled Struct.* **2023**, *182*, 110249. [\[CrossRef\]](#)
3. Zhou, X.; Tan, Y.; Ke, K.; Yam, M.C.; Zhang, H.; Xu, J. An experimental and numerical study of brace-type long double C-section steel slit dampers. *J. Build. Eng.* **2023**, *64*, 105555. [\[CrossRef\]](#)
4. Liao, F.; Liu, Y.; Wang, W.; Lu, Z.; Li, X. Fracture performance study on Q460D steel and ER55-G welds after high temperature. *J. Constr. Steel Res.* **2023**, *206*, 107888. [\[CrossRef\]](#)
5. Chiew, S.P.; Zhao, M.S.; Lee, C.K. Mechanical properties of heat-treated high strength steel under fire/post-fire conditions. *J. Constr. Steel Res.* **2014**, *98*, 12–19. [\[CrossRef\]](#)
6. Heidarpour, A.; Tofts, N.S.; Korayem, A.H.; Zhao, X.L.; Hutchinson, C.R. Mechanical properties of very high strength steel at elevated temperatures. *Fire Saf. J.* **2014**, *64*, 27–35. [\[CrossRef\]](#)
7. Wang, W.Y.; Li, G.Q. Research progress on fire resistance design theory of high strength steel structures. *Ind. Constr.* **2016**, *46*, 61–67. (In Chinese)
8. Wang, K. *Experimental Study on Mechanical Properties of High-Strength Q690 Steel at Elevated Temperatures*; Chongqing University: Chongqing, China, 2016. (In Chinese)
9. Abuhishmeh, K.; Jalali, H.H.; Ebrahimi, M.; Soltanianfard, M.A.; Correa, C.O.; Cornejo, J.S. Behavior of high strength reinforcing steel rebars after high temperature exposure: Tensile properties and bond behavior using pull-out and end beam tests. *Eng. Struct.* **2024**, *305*, 117730. [\[CrossRef\]](#)
10. Yamaguchi, T.; Ozaki, F. Tensile strengths of super high-strength steel strand wire ropes and wire rope open swaged socket connections at fire and post fire. *J. Struct. Fire* **2024**, *15*, 50–75. [\[CrossRef\]](#)
11. Pandey, M.; Young, B. Cold-formed high strength steel CHS-to-RHS T- and X-joints: Performance and design after fire exposures. *Thin-Walled Struct.* **2023**, *189*, 110793. [\[CrossRef\]](#)
12. Pandey, M.; Young, B. Post-fire behaviour of cold-formed high strength steel tubular T- and X-joints. *J. Constr. Steel Res.* **2021**, *186*, 106859. [\[CrossRef\]](#)
13. Zhuang, Z.; Jiang, Z.P. *Engineering Fractures and Damage*; China Machine Press: Beijing, China, 2004.
14. Kanvinde, A.M.; Deierlein, G.G. Void growth model and stress modified critical strain model to predict ductile fracture in structural steels. *J. Struct. Eng. ASCE* **2006**, *132*, 1907–1918. [\[CrossRef\]](#)
15. Rice, J.R.; Tracey, D.M. On the ductile enlargement of voids in triaxial stress fields. *J. Mech. Phys. Solids* **1969**, *17*, 201–217. [\[CrossRef\]](#)
16. Hancock, J.W.; Mackenzie, A.C. On the mechanics of ductile failure in high-strength steel subjected to multi-axial stress states. *J. Mech. Phys. Solids* **1976**, *24*, 147–169. [\[CrossRef\]](#)
17. Kanvinde, A.M.; Deierlein, G.G. *Micromechanical Simulation of Earthquake-Induced Fracture in Steel Structures*; Stanford University: Stanford, CA, USA, 2004.
18. Kanvinde, A.M.; Deierlein, G.G. Prediction of Ductile Fracture in Steel Moment Connections During Earthquakes Using Micromechanical Fracture Models. In Proceedings of the 13th World Conference on Earthquake Engineering, Vancouver, BC, Canada, 1–6 August 2004; Volume 297.
19. Kanvinde, A.M.; Deierlein, G.G. Finite-element simulation of ductile fracture in reduced section pull-plates using micromechanics-based fracture models. *J. Struct. Engineering* **2007**, *133*, 656–664. [\[CrossRef\]](#)
20. Roufegarinejad, A.; Tremblay, R. Finite element modeling of the inelastic cyclic response and fracture life of square tubular steel bracing members subjected to seismic inelastic loading. In *Behavior of Steel Structures in Seismic Areas (STESSA 2012)*; CRC Press: Boca Raton, FL, USA, 2012; pp. 97–103.
21. Siriwardane, S.; Ratnayake, R.M.C. A Simple criterion to predict fracture of offshore steel structures in extremely-low cycle fatigue region. In Proceedings of the International Conference on Offshore Mechanics and Arctic Engineering, Rio de Janeiro, Brazil, 1–6 July 2012; pp. 211–218.
22. Wang, Y.; Zhou, H.; Shi, Y.; Xiong, J. Fracture prediction of welded steel connections using traditional fracture mechanics and calibrated micromechanics based models. *Int. J. Steel Struct.* **2011**, *11*, 351–366. [\[CrossRef\]](#)
23. Liao, F.F.; Wang, W.; Chen, Y.Y. Parameter calibrations and application of micromechanical fracture models of structural steels. *Struct. Eng. Mech.* **2012**, *42*, 153–174. [\[CrossRef\]](#)
24. Liao, F.F.; Wang, R.Z.; Li, W.C.; Zhou, T.H. Study on micro mechanism-based ductile fracture criteria for Q460 steel. *J. Xi'an Univ. Archit. Technol. (Nat. Sci. Ed.)* **2016**, *48*, 535–543+550. (In Chinese)
25. Liao, F.; Wang, M.; Tu, L.; Wang, J.; Lu, L. Micromechanical fracture model parameter influencing factor study of structural steels and welding materials. *Constr. Build. Mater.* **2019**, *215*, 898–917. [\[CrossRef\]](#)
26. Liao, F.F.; Wang, W.; Chen, Y.Y. Extremely low cycle fatigue fracture prediction of steel connections under cyclic loading. *J. Tongji Univ. (Nat. Sci.)* **2014**, *42*, 539–546+617. (In Chinese)
27. Li, K.K. *Study on Extremelylow Cycle Fatigue Fracture Performance of Q460 High-Strength Steel Welded Beam-Column Connections with Different Welding Hole Structures*; Chang'an University: Xi'an, China, 2020. (In Chinese)
28. Yu, Z.W.; Wang, Z.Q.; Shi, Z.F. Experimental research on material properties of new III grade steel bars after fire. *J. Build. Struct.* **2005**, *26*, 112–116. (In Chinese)

29. GB 50661-2011; Code for Welding of Steel Structures. China Building Industry Press: Beijing, China, 2011. (In Chinese)
30. Lemaitre, J.; Chaboche, J.L. Mechanics of Solid Materials. Master's Thesis, Cambridge University Press, Cambridge, UK, 1990.

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