Materials Research Express

**PAPER**

Ductile fracture behavior and flow stress modeling of 17-4PH martensitic stainless steel in tensile deformation at high temperature

Chao Feng, Li Zhang, Jian Wu and Hang Yu

1 School of Mechanical and Aerospace Engineering, Jilin University, Changchun, 130022, People’s Republic of China
2 School of Chemistry and Materials Science, Ludong University, Yantai, 264025, People’s Republic of China

E-mail: fco@jlu.edu.cn

**Keywords:** 17-4PH steel, fracture, hot tensile deformation, modified Johnson-Cook model

**Abstract**

The ductile fracture behavior of 17-4PH martensitic stainless steel at high temperature was investigated based on the experimental data of hot uniaxial tensile test. The elongation and reduction of area indicated that the ductility was mainly enhanced during the deformation at the temperature 1150 °C–1200 °C with high strain rate more than 0.1 s\(^{-1}\). The hot flow stress curves were obtained from the tensile experiments, and the true strain of necking initiation was determined by analyzing the contours of fractured specimens. Then, the true stress-true strain data of the deformations before necking were adopted to establish the tensile flow stress model. In order to express the combined influences of the strain, strain rate and temperature on flow stress, a modified Johnson-Cook model was proposed to describe the hot constitutive behavior of the presented steel. It indicates that reasonable agreements between the model-predicted results and the experimental data were achieved.

---

**1. Introduction**

With the outstanding characteristics of anti-corrosion, resist-to-abrasion and high mechanical strength, 17-4PH martensitic stainless steel is widely used in structural components of the key equipment in many fields, such as marine construction, chemical industries and power plants [1]. Generally, the hot forming process is an effective method to form the shape and reﬁne the grains for the parts made of 17-4PH steel. Meanwhile, the good heat-treatment strengthening property of this steel also promotes the applicability of hot working process [2–4]. To improve the mechanical properties, it is important to understand the hot ductility behavior and plastic flow behavior of the steel to guide the process planning and avoid the fracture during the hot forming process. Trzpiecinski et al [5] realized the forming process of the engine strut made of 17–4PH steel at the temperature range of 20 °C–800 °C, and the formability and microstructure evolution were investigated. Su et al [6] investigated the tensile flow behavior of 17–4PH steel sheet under the temperature from 650 °C to 850 °C at strain rates of 10\(^{-3}\) s\(^{-1}\) to 10\(^{-1}\) s\(^{-1}\). Generally, in the hot forming of 17–4PH steel, especially for the forging of the large ingot, a relatively high temperature at the beginning of forging process is required and the multi-stage deformation with long processing period is performed, which causes the steel to deform over a wide range of temperature during the entire deformation process. Moreover, the distributions of strain and stain rate in the large ingot vary complicatedly due to the multi-stage incremental deformation history. Considering the ductility of steels is influenced significantly by strain rate and temperature [7–9], further investigations are required to understand the fracture behavior of 17–4PH steel at high temperature.

In order to develop the reliability of the hot forming process, the finite element (FE) simulation can be an effective approach to promote the forging technology with shape-controlling and property-controlling strategies. To carry out an effective evaluation of the material flow behavior and the stress/strain distribution, it is essential to establish a precise constitutive equation to describe the features of the flow stress variation during the hot forming process [10–12]. Several kinds of constitutive models, including the physical-based models and phenomenological models, were proposed to express the true stress–true strain relationships, and the accuracy
and applicability were investigated. The physical-based models are usually established on the framework of deformation mechanism, which take into account the evolution of dislocation density and recrystallization kinetics [13–15]. However, the precise tests are often required to determine a large amount of parameters critically to establish the physical-based models. The phenomenological models including Johnson-Cook (J-C) model [16], Arrhenius equation [17], etc, are mainly based on the mathematical equations to fit or regress the experimental data. With a wide range of applications, J-C model has a concise framework, and it is appropriate to describe the combined effects of the strain, strain rate and temperature on the flow stress. Many studies modified the J-C model to improve the applicability in different materials and achieved the reasonable agreements [18–21]. In literatures about 17–4PH steel, the constitutive model and recrystallization model have been established for hot compressive deformation [22–24], however, the researches about the modeling of the tensile flow stress at high temperature are still lacking.

In this paper, the hot tensile deformation test was carried out to determine the fracture behavior of 17–4PH martensitic stainless steel, and the influences of temperature and strain rate on the ductility were carefully investigated to provide the guidance for the processing parameter designing of the hot deformation process. Meanwhile, a modified J-C model was proposed to describe the tensile flow stress behavior of the presented steel during the hot deformation.

2. Experimental procedures

The testing specimens were designed and machined with the diameter of 10 mm and the gauge length of 20 mm, and the uniaxial hot tensile tests were carried out by the Gleeble1500D thermo-mechanical simulator as shown in figure 1 and figure 2. Each specimen was heated to the deformation temperature at a rate of 20 °C/s by thermo-coupled feedback-controlled AC current, and held for 3 min at isothermal conditions before the deformation to obtain the heat balance. The uniaxial tensile tests were performed at various temperatures (950 °C, 1050 °C, 1150 °C, 1200 °C, 1250 °C) and strain rates(0.01 s⁻¹, 0.1 s⁻¹, 1 s⁻¹, 5.0 s⁻¹) in order to determine the hot fracture behaviors.

3. Results and discussions

3.1. Ductile fracture behavior

Figure 3 shows the typical specimens after the hot tensile tests, and it demonstrates that necking phenomenon occurred before fracture. The deformation temperature and strain rate influence the fracture behavior of this steel significantly. The elongation of the specimens is illustrated in figure 4(a). It indicates that the ductility of 17–4PH mainly increases with the rising of strain rate at the temperature range from 950 °C to 1250 °C. Meanwhile, the elongation of the specimens is influenced by the deformation temperature more significantly. The fracture behavior at low temperature(950 °C) shows relatively poor ductility with the minimum elongation 40%(at the strain rate 0.01 s⁻¹). With the rising of the temperature, the elongation shows an ascendant trend at the temperature range from 950 °C to 1150 °C and then obviously descends when the temperature is higher than 1200 °C. Figure 4(b) shows the necking surfaces of the specimens after the tensile tests, it can be seen that the reduction of area at the fracture zone increases with the temperature at the temperature range from 950 °C to 1150 °C, and then decreases when the deformation temperature reaches 1250 °C. The elongation and the
reduction of area of the specimens exhibit the similar variation of the ductility of this steel. According to figures 4(a) and (b), the hot deformation of this steel is preferred to be carried out at the temperature range from 1150 °C to 1200 °C under the strain rate more than 0.1 s⁻¹ to obtain a high ductility.

SEM photographs and metallographs of the fracture morphologies are demonstrated in figure 5. The specimens exhibit typical ductile fracture characteristics at different tensile conditions. It is notable that the fracture surfaces of specimens at different strain rate show dissimilar appearances. In the specimens deformed at the low strain rate (ε = 0.01 s⁻¹), although the voids and dimples appear in the low-magnified images (figures 5(a)–(c)), scarcely any sharp tearing edges are observed in the high-magnified images (figures 5(d)–(f)). Moreover, the sizes of dimples are non-uniform. In the specimens deformed at the high strain rate (ε = 5 s⁻¹), more ligament structures with relatively-uniform dimples appear both in low-magnified and high-magnified images of the fracture surfaces (figures 5(g)–(l)). The metallographic photographs of the longitudinal section
near the fractures in the specimens deformed under different strain rates are demonstrated in figures 5(m)–(o), which show that a small amount of voids with large volumes exist in the specimen deformed under the low strain rate, while a large number of tiny voids appear in the specimen deformed under the high strain rate. During the hot tensile deformation with low strain rate, it gives time to eliminate the pile-up of dislocations and soften the material by dynamic recovery and recrystallization, which makes the void growth mechanism predominate over the void initiation. Thus the growing of the voids is conspicuous and finally leads to the catastrophic fracture in the tensile deformation under the low strain rate. Typical SEM photographs of the fracture in the specimens: (a) 950 °C, 0.01 s⁻¹, (b) 1050 °C, 0.01 s⁻¹, (c) 1150 °C, 0.01 s⁻¹, (g) 950 °C, 5 s⁻¹, (h) 1050 °C, 5 s⁻¹, (i) 1150 °C, 5 s⁻¹, (d), (e), (f), (k), (l) are the magnified images of (a), (b), (c), (g), (h) and (i), respectively. The optical metallographs of the micro-voids near the fracture in the specimens: (m) 1150 °C, 0.01 s⁻¹, (n) 1150 °C, 0.1 s⁻¹, (o) 1150 °C, 5 s⁻¹.

Figure 5. SEM photographs of fracture appearance in the specimens: (a) 950 °C, 0.01 s⁻¹, (b) 1050 °C, 0.01 s⁻¹, (c) 1150 °C, 0.01 s⁻¹, (g) 950 °C, 5 s⁻¹, (h) 1050 °C, 5 s⁻¹, (i) 1150 °C, 5 s⁻¹, (d), (e), (f), (k), (l) are the magnified images of (a), (b), (c), (g), (h) and (i), respectively. The optical metallographs of the micro-voids near the fracture in the specimens: (m) 1150 °C, 0.01 s⁻¹, (n) 1150 °C, 0.1 s⁻¹, (o) 1150 °C, 5 s⁻¹.
variation are observed in the specimens deformed under other conditions, as shown in figure 6(f). Moreover, under the same deformation temperature, the average size of dimples in the specimens under the low strain rate is much larger than that in the specimens under the high strain rate, which also indicates that the effect of void growth on the fracture mechanism enhances with the decreasing of the strain rate.

3.2. Flow stress modeling

3.2.1. Flow stress curves and determination of the initial necking strain

As shown in figure 7, the true stress–true strain curves were obtained from the tensile test results, which represent the combined effect of the strain, strain rate and temperature on the changing of flow stress. It can be seen that the flow stress rises with the decreasing of temperature and the increasing of the strain rate. During the uniaxial tensile tests, the true strain is obtained as $\varepsilon = \ln (l/l_0)$, in which $l$ represents the instantaneous gauge length during the deformation and $l_0$ is the original gauge length before deformation. However, the necking phenomenon, which often occurs before the ductile fracture in the tensile deformation, causes the deformation concentrated in a local region. After necking, the correctness of the true strain calculated by $\varepsilon = \ln (l/l_0)$ is deteriorated significantly. In order to determine the valid range of the experimental true stress–true strain curves, the strain of necking initiation in the tensile deformation under different conditions should be specified.

As shown in figure 7, it is difficult to directly determine the strain of necking initiation from the flow stress data measured in the experiments, because the flow stress in hot tensile test descends smoothly after reaching the
peak stress. A novel method is proposed to find the initial necking strain during the hot tensile deformation. Considering the deformation is uniform before necking, if the instantaneous situation of the maximum uniform deformation can be determined, then the strain of necking initiation is obtained. As shown in figure 8(a), the image of the fractured specimen was captured by the stereoscopic microscope. The red solid curve between point A and C represents the final contour of the half part of the deformed specimen after fracture, which is extracted by image treatment method. Obviously, the final contour between point A and B can be approximately treated as straight line, which indicates the deformation is uniform and no necking occurs. Meanwhile, the contour between point B and C is curve, which indicates the necking with deformation concentration occurs. The yellow dash line between point A and D in figure 8(a) is the virtual equivalent contour which exhibits the instantaneous state when necking is initializing. The equivalent contour between point A and B coincides with the red final contour line, and the contour between point B and D is an extended line of AB. Certainly, the volume of the specimen with the yellow equivalent contour should be equal to the volume with the red final contour. Based on the image and length scale in figure 8(a), the Cartesian coordinate system has been established with the zero point locating on the intersection of the original gauge line and the axial line of the half part of the fractured specimen. Then, the red final contour and the yellow equivalent contour can be fitted as \( f(x) \) and \( F(x) \), respectively. When necking is initializing, the volume of the gauge region is expressed as \( \pi \int_0^{l_f} F^2(x) \, dx \).

While the specimen is fractured, that volume is expressed as \( \pi \int_0^{l_i} f^2(x) \, dx \). According to the volume-invariant criterion, the following equation is specified as:

\[
V = \pi \int_0^{l_f} f^2(x) \, dx = \pi \int_0^{l_i} F^2(x) \, dx
\]

where \( V \) represents the volume of the half part of the fractured specimen in the gauge region. \( l_i \) is the length between the gauge line to the fracture tip of the half part of the fractured specimen, and \( l_f \) represents the equivalent length at the instantaneous state when necking is initializing. In this study, \( f(x) \) is approximately fitted as a cubic polynomial and denoted as \( f(x) = a_1x^3 + a_2x^2 + a_3x + a_4 \). \( F(x) \) is fitted as a linear polynomial and expressed as \( F(x) = b_1x + b_2 \). In the polynomials, \( a_1, a_2, a_3, b_1, b_2 \) are the fitted parameters of \( f(x) \) and \( F(x) \). Then, equation (1) is rewritten as:

\[
\int_0^{l_i} (a_1x^3 + a_2x^2 + a_3x) \, dx = \int_0^{l_i} (b_1x + b_2)^2 \, dx
\]

and then expanded as

\[
\frac{b_1}{3}l_i^3 + \frac{b_1b_2}{2}l_i^2 + b_2^2l_i = \left[ \frac{a_1}{5}l_i^5 + \frac{a_2}{2}l_i^4 + \frac{a_3}{3}l_i^3 + \frac{2a_4}{3}l_i^2 + a_2b_1l_i + b_2^2 \right] = 0
\]

Generally, \( a_1, a_2, a_3, b_1, b_2 \) are constants and can be determined from the contour fitting of the specimen as shown in figure 8(a). \( l_i \) can be obtained from the measurement in the image of the specimen. Thus, equation (3) becomes a cubic equation with one variable \( l_i \). Thus, the value of \( l_i \) can be solved according to the famous Cardano formula. Using the same method, the equivalent length in the other half part of the fractured specimen is determined and denoted as \( l_f \). Ultimately, the true strain at necking initiation can be specified as

\[
\varepsilon_n = \ln \left( \frac{l_i + l_f}{l_0} \right)
\]

According to the results of tensile experiments, the values of \( \varepsilon_n \) for the deformations
under different conditions have been obtained, as shown in figure 8(b). It exhibits that the value of $\varepsilon_n$ increasing with the deformation rate. Meanwhile, at the range from 950 °C to 1250 °C, $\varepsilon_n$ firstly rises and then descends with the increasing of temperature. The greater values of $\varepsilon_n$ appear in the deformations at the temperature near 1150 °C, which indicates that the presented steel deformed at these conditions can provide more uniform deformation and better ductile performances. The strain of necking initiation is an important parameter to characterize the instability and fracture behavior, which can also be adopted in the investigation of hot fracture modeling.

Based on the values of $\varepsilon_n$ determined above, the valid range of the flow stress curves representing the true stress-true strain relations can be distinguished from the measured results in tensile experiments. As shown in figure 7, the solid lines represent the true stress-true strain relations before necking, while the dash lines after the necking initiation points may deviate to the real relations between true stress and true strain. Besides, the area under the stress-strain curve is related the absorbed energy of the steel in its unit volume during the tensile deformation, and it can generally represent the toughness of the material [25–27]. In this study, the area under the stress-strain curve before necking was calculated and exhibited in figure 9. It demonstrates that the absorbed energy mainly increases with the decreasing of deformation temperature and the increasing of strain rate. The peak value of absorbed energy before necking is 59.8 J/m³ and appears in the tensile deformation under the condition of (1050 °C, 5 s⁻¹).

3.2.2. Flow stress modeling
As shown in figure 7, the valid ranges of the flow stress curves before necking were adopted to establish the flow stress constitutive model at high temperature for 17-4PH steel. According to the experimental data, the value of flow stress is significantly influenced by strain rate and temperature. In order to reflect the combined effects of temperature, strain rate and strain on the flow stress at hot deformation, the conventional J-C model was applied to describe the flow behavior of this steel at first. The conventional J-C model is defined as [16]

$$
\sigma = (A + B\dot{\varepsilon}^n)(1 + C \ln \dot{\varepsilon}^*) (1 - T^{*m})
$$

(4)

where $\sigma$ is Von Misses flow stress, and $\varepsilon$ is the effective plastic strain. $\dot{\varepsilon}^* = \dot{\varepsilon}/\dot{\varepsilon}_r$ is the dimensionless strain rate, in which $\dot{\varepsilon}$ is the strain rate and $\dot{\varepsilon}_r$ is the reference strain rate. $T^* = (T - T_r)/(T_m - T_r)$ in which $T, T_r$ and $T_m$ are the deformation temperature, reference temperature and melting temperature, respectively. For this steel, $T_r = 950$ °C and $T_m = 1420$ °C. $A, B, C, m$ and $n$ are the modeling constants, which can be determined on the basis of the experimental flow stress data. When $\dot{\varepsilon} = \dot{\varepsilon}_r$ and $T = T_r$, the equation (4) can be expressed as following:

$$
\sigma = A + B\dot{\varepsilon}^n
$$

(5)

The reference flow stress curve, which was obtained from the experiment under deformation condition of 950 °C and 5 s⁻¹, was adopted to determine the parameters in equation (5). When $\dot{\varepsilon}$ is zero, $A$ is equal to the initial yield stress $\sigma_0$, which is 36 MPa. Then, equation (5) can be deduced as

$$
\ln (\sigma - A) = \ln B + n \ln \varepsilon
$$

(6)
According to equation (6), the relationship of $\ln(\sigma - A)$ and $\ln \varepsilon$ can be linearly fitted, with the slope and intercept of the fitted line equal to $n$ and $\ln B$, respectively. As shown in figure 10, the relationship of $\ln(\sigma - A)$ versus $\ln \varepsilon$ was fitted, and the parameters were determined as $n = 0.4895$ and $B = 498.35$.

When the strain rate is constant, equation (4) can be deduced as

$$
\ln \left(1 - \frac{\sigma}{A + B\varepsilon^m}\right) = m \ln T^*
$$

(7)

The experimental data at different temperatures and $\dot{\varepsilon} = 5\text{ s}^{-1}$ were used to determine the parameter $m$. The relationship between $\ln \left[1 - \sigma/(A + B\varepsilon^m)\right]$ and $\ln T^*$ are fitted as shown in figure 11, and the slope of the fitted average line equals to the value of parameter $m$, namely, $m = 1.7821$.

When the deformation temperature is equal to reference temperature, equation (4) can be deduced as

$$
\frac{\sigma}{A + B\varepsilon^m} = 1 + C \ln \dot{\varepsilon}^*
$$

(8)

The experimental data of the tensile deformation at 950°C with different strain rates were used to determine the parameter $C$. As shown in figure 12, the relationship between $\sigma/(A + B\varepsilon^m)$ and $\ln \dot{\varepsilon}^*$ is demonstrated. $C$ was equal to the average value of the slopes of the fitted lines, which was 0.08146.

Above all, the conventional J-C model for 17–4PH steel under hot tensile deformation has been established as following:

$$
\sigma = (36 + 498.35\varepsilon^{0.4895})(1 + 0.08146 \ln \dot{\varepsilon}^*)(1 - T^{1.7821})
$$

(9)

The comparison between the calculated result of the conventional J-C model and the experimental data is shown in figure 13. It can be seen that the conventional J-C model is not quite suitable to describe the flow stress features of the presented steel at high temperature. The expression of strain hardening, strain rate hardening and thermal softening are independent and can be isolated from each other in the conventional J-C model, however,
in the hot deformation for steels, the influences of the factors including strain, strain rate and temperature on the flow stress are combined and hard to separate [20]. To solve this problem, a modified J-C model was proposed in this section to describe the flow stress behavior of 17–4PH steel at high temperature.

The modified J-C model was proposed as

$$\sigma = A_0 \varepsilon^\alpha (1 + \beta \ln \sigma)(1 + \gamma \ln \dot{\varepsilon}^* + 0.1 \ln \dot{\varepsilon}^* + 0.1 T^* + T^{*2})$$

Where $A_0$, $\alpha$, $\beta$, $\gamma$ and $\eta$ are the model parameters which reflect the influences of strain, strain rate and temperature on the flow stress. When the temperature and strain rate are constants, the modified J-C model can be denoted as

Figure 12. Relationship between $\sigma/(A + B\dot{\varepsilon}^*)$ and $\ln \dot{\varepsilon}^*$ of the experiments at $\dot{\varepsilon} = 5$ s$^{-1}$. 

Figure 13. Comparison between the calculated result of the conventional J-C model and the experimental data at the deformation conditions of (a) 5 s$^{-1}$, (b) 1 s$^{-1}$, (c) 0.1 s$^{-1}$ and (d) 0.01 s$^{-1}$.
The experimental data at the condition of \((950\, ^\circ\text{C}, 5\, \text{s}^{-1})\) were applied to determine the values of parameters in equation \((11)\). As shown in figure 14, the relationship between \(\ln \sigma\) and \(\ln \varepsilon\) is demonstrated and a second-order polynomial was adopted to fit the data. According to the fitted polynomial, the values of the parameters can be determined as \(A_1 = 312.25\), \(\alpha = 0.04596\) and \(\beta = -1.1105\).

When the strain rate is equal to the reference strain rate, the modified J-C model can be deduced as

\[
\ln \sigma = \ln A_1 + \alpha \ln \varepsilon + \alpha \beta (\ln \varepsilon)^2
\]  

The experimental data at the condition of \((950\, ^\circ\text{C}, 5\, \text{s}^{-1})\) were applied to determine the values of parameters in equation \((11)\). As shown in figure 14, the relationship between \(\ln \sigma\) and \(\ln \varepsilon\) is demonstrated and a second-order polynomial was adopted to fit the data. According to the fitted polynomial, the values of the parameters can be determined as \(A_1 = 312.25\), \(\alpha = 0.04596\) and \(\beta = -1.1105\).

When the strain rate is equal to the reference strain rate, the modified J-C model can be deduced as

\[
\frac{\sigma}{A_1 \varepsilon^{\alpha(1 + \beta \ln \varepsilon)}} = 1 + \eta T^* + T^{*2}
\]  

The experimental data at different temperatures and \(\dot{\varepsilon} = 5\, \text{s}^{-1}\) were used to determine the parameter \(\eta\). The relationship between \(\sigma/[A_1 \varepsilon^{\alpha(1 + \beta \ln \varepsilon)}]\) and \(T^*\) was demonstrated in figure 15, and a second-order polynomial was adopted to fit the data. According to the fitted results, the value of \(\eta\) was obtained as \(-1.68\).

According to equation \((10)\), the modified J-C model can be deduced as

\[
\frac{\sigma}{A_1 \varepsilon^{\alpha(1 + \beta \ln \varepsilon)}} = (1 + 0.1 \ln \dot{\varepsilon}^*) \varepsilon^{\alpha(1 + \beta \ln \varepsilon)} \ln \dot{\varepsilon}^*
\]  

Take \(X = \varepsilon^{\alpha(1 + \beta \ln \varepsilon)}\) and \(Y = \sigma/[A_1 \varepsilon^{\alpha(1 + \beta \ln \varepsilon)}(1 + \eta T^* + T^{*2})]\), considering the values of \(A_1\), \(\alpha\), \(\beta\) and \(\eta\) have been determined above, the values of \(X\) and \(Y\) can be obtained on the basis of the experimental flow stress data. Then, equation \((13)\) can be written as

\[
\ln \left(1 + 0.1 \ln \dot{\varepsilon}^*\right) = \gamma \ln \dot{\varepsilon}^* \ln X
\]  

Firstly, the experimental data of the tensile deformation at \((950\, ^\circ\text{C}, 1\, \text{s}^{-1})\) were adopted to determine the values of parameters in equation \((14)\). With \(\dot{\varepsilon}^* = 1/5\), equation \((14)\) is expressed as
Then, the relationship between $\ln X$ and $\ln (Y/0.839)$ was demonstrated in figure 16, and can be linearly fitted on the basis of equation (15). The slope of the fitted line was equal to $-1.61\gamma$. Thus, the value of $\gamma$ for the deformation condition of $(950^\circ C, 1\ s^{-1})$ were determined. Using the similar procedure, the slopes of the fitted lines for tensile deformations at the rest of experimental conditions were also calculated and the corresponding values of $\gamma$ were specified. Based on the fitted results of the experimental data at different deformation conditions, the average value of $\gamma$ was determined as 0.1035, ultimately.

Finally, the modified J-C model has been established as following:

$$\ln \left( \frac{Y}{0.839} \right) = -1.61\gamma \ln X$$

(15)

In figure 17, the comparison between calculated data by the modified J-C model and the experimental flow stress data are demonstrated, and good agreements are achieved under the deformation conditions with different temperatures and strain rates. To further verify the applicability of the presented model, the normalized bias error (NMBE) method was adopted to compare the accuracy of the modified J-C model and the conventional J-C model. The NMBE is denoted as

$$\text{RMSE} = \sqrt{\frac{1}{N} \sum_{i=1}^{N} (E_i - P_i)^2}$$

(17)

where RMSE is the average root mean square error, $E_i$ and $P_i$ are the experimental data and the predicted values, respectively. $N$ is the amount of data used in the investigation. Based on equation (17), RMSE of the modified J-C model and the conventional J-C model at different deformation conditions were calculated as shown in table 1. The RMSE of the modified J-C model is much smaller than the conventional JC model, which indicates that a higher accuracy is achieved by using the modified J-C model.

4. Conclusions

In this work, the ductile fracture behaviors of $17\text{--}4\text{PH}$ steel at the temperature of $950 \text{--} 1250^\circ C$ and strain rate of $0.01 \text{--} 5 \text{~s}^{-1}$ were investigated. The elongation and reduction of area indicate that the ductility of this steel is mainly enhanced during the deformation at the temperature $1150 \text{--} 1200^\circ C$ with a relatively high strain rate more than $0.1 \text{~s}^{-1}$, while deformation with low temperature and low strain rate cause the relatively poor ductility. The true strain of necking initiation $\varepsilon_n$ in hot tensile deformation of $17\text{--}4\text{PH}$ steel was determined according to the image treatment of the fractured specimens, thus, the valid range of the true stress-true strain data measured in the experiments were determined. A modified J-C model was proposed to describe the flow stress behavior of $17\text{--}4\text{PH}$ steel at high temperature, and the true stress-true strain data measured in the deformation before necking were adopted to establish the presented model. It indicates that reasonable agreements have been achieved between the model-predicted results and the experimental flow stress data.
Acknowledgments

The authors gratefully acknowledge the financial support from National Natural Science Foundation of China (Grant No. 51805204), Postdoctoral Science Foundation of China (Grant No. 2017M621208) and Education Department of Jilin Province in China (Grant No. JJKH20200976KJ).

ORCID iDs

Chao Feng https://orcid.org/0000-0002-1377-0587

References

[1] Wu M W et al 2015 Met. Mater. Int. 21 531–7
[2] Chung C and Tzeng Y 2019 Mater. Lett. 237 228–31
[3] Razavi S A, Ashrafizadeh F and Fooladi S 2016 Mater. Sci. Eng. A 675 147–52
[4] Mirzadeh H and Najafizadeh A 2009 Mater. Chem. Phys. 116 119–24
[5] Trzepieciński T et al 2018 J. Mater. Process. Technol. 252 191–200

Figure 17. Comparison between the calculated results of the modified J-C model and the experimental data at the deformation conditions of (a) 5 s⁻¹, (b) 1 s⁻¹, (c) 0.1 s⁻¹ and (d) 0.01 s⁻¹.

Table 1. RSME of the modified J-C model (MJCM) and conventional J-C model (CJCM) at different deformation conditions.

| Temperature | 950 °C | 1050 °C | 1150 °C | 1200 °C | 1250 °C |
|-------------|--------|--------|--------|--------|--------|
| ε = 5s⁻¹ | 15.94  | 4.894  | 3.394  | 6.960  | 6.831  | 15.77  | 3.638 |
| ε = 1s⁻¹ | 27.56  | 10.39  | 5.59   | 4.448  | 4.104  | 42.82  | 2.751 |
| ε = 0.1s⁻¹ | 23.44  | 5.212  | 7.111  | 4.919  | 2.501  | 14.59  | 8.286 |
| ε = 0.01s⁻¹ | 31.54  | 11.491 | 26.24  | 1.568  | 2.239  | 19.28  | 1.762 |
|             | 9.109  | 2.593  | 10      | 3.57   | 2.239  | 19.28  | 1.762 |

Mater. Res. Express 7 (2020) 046503 C Feng et al
