Thermal shock of subsurface material with plastic flow during scuffing

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Abstract: The thermal shock of subsurface material with shear instability and severe plastic flow during scuffing was investigated. The scuffing damage of M50 steel was tested using a high-speed rolling–sliding contact test rig, and the transient temperature during scuffing was calculated using the Fourier transform method considering the effects of both frictional heat and plastic work. The results show that a thermal shock with a rapid rise and subsequent rapid decrease in the contact temperature is generated in the subsurface layers. The frictional power intensity generates a high temperature rise, leading to the austenitization of the subsurface material. Consequently, the plastic flow is generated in the subsurface layer under the high shear stress, and the resulting plastic strain energy generates a further temperature increase. Subsequently, a rapid decrease in the contact temperature quenches the material, resulting in clear shear slip bands and retained austenite in the subsurface layers of the M50 steel.

Keywords: scuffing; thermal shock; high temperature; plastic flow

1 Introduction

In mechanical systems operating under extreme conditions, contact interfaces generally endure various types of surface damage due to ubiquitous friction [1–5]. Among them, scuffing is a catastrophic surface damage that occurs under high speeds and heavy loads. Complex thermomechanical processes and material reactions, that is, a rapid rise in the contact temperature and frictional force, sudden shear instability with severe plastic flow in subsurface layers, and welding of contact surfaces, are general characteristics of scuffing damage [6–8]. However, the interaction between thermomechanical coupling and the material response during the scuffing process remains elusive.

The contact temperature due to frictional heat was postulated to be one of the crucial factors in scuffing damage. Blok [9] calculated the actual contact surface temperature using the heat-source method. A critical contact temperature was postulated to predict scuffing. Based on this earliest attempt, various theoretical models have been developed for calculating the contact temperature rise [10–14]. Tian and Kennedy [13] analyzed the temperature rise at sliding contacts using the Fourier transform method and obtained the contact temperatures at different Peclet numbers. Laraqi et al. [14] obtained the exact temperature rise at pin-on-disk contacts using the Hankel transform method. Coulibaly et al. [15] analyzed the high-velocity contact temperature on rough surfaces using a Green's function. Waddad et al. [16] analyzed the local surface temperature at rough contacts using the fast Fourier transform method considering the effects of wear debris.

Adiabatic shear bands with severe plastic flow in subsurface layers have been suggested to be the root cause of scuffing damage [17]. According to adiabatic shear instability theory [18–21], the plastic work in
## Nomenclature

| Symbol | Description |
|--------|-------------|
| $a$ | Contact radius |
| $B_j$ | Gauss–Hermite integration coefficients |
| $c$ | Specific heat capacity of the material |
| $C_j$ | Gauss–Hermite integration coefficients |
| $C_{VP}$ | Lubricant viscosity pressure coefficient |
| $C_{VT}$ | Lubricant viscosity temperature coefficient |
| $D_{\text{min}}$ | Minimum lubricant film thickness |
| $E_T$ | Tangential modulus |
| $G_o$ | Material-dependent parameter of the oil film thickness |
| $G_j$ | Gauss–Hermite integration function |
| $h$ | Characteristic depth of the plastic deformation layer |
| $H$ | Normalized characteristic depth of the plastic deformation layer, $\frac{h}{2a}$ |
| $k$ | Heat conduction of the material |
| $k_e$ | Ellipticity ratio |
| $K_{s1}$ | Normalized coefficient, $\frac{2\alpha\beta\gamma_0n(\mu p_{\text{max}})^{1/\alpha+1}}{r_0^{1/\alpha}q_0t_0(n+1)}$ |
| $K_{s2}$ | Normalized coefficient, $\frac{2\alpha\beta(\mu p_{\text{max}})^2}{q_0t_0E_T}$ |
| $M_{si}$ | Parameter in the Fourier transform equations, $\frac{K_s}{4P_e}$ |
| $m$ | Fitting value for the material properties |
| $n$ | Work hardening coefficient |
| $p_{\text{max}}$ | Maximum contact pressure |
| $p_{\text{scuffing}}$ | Hertzian contact pressure at scuffing |
| $P_e$ | Peclet number |
| $q$ | Frictional power intensity |
| $q_0$ | Maximum frictional power intensity |
| $Q^*$ | Normalized frictional power intensity, $\frac{q}{q_0}$ |
| $r$ | Radius of disk 1 in axial direction |
| $R$ | Radius of the disk in rolling direction |
| $R_s$ | Surface roughness of the disk |
| $s$ | Normalized shear stress, $\frac{\tau_s}{\mu p_{\text{max}}}$ |
| $t_0$ | Contact time |
| $T_b$ | Maximum bulk temperature during scuffing |
| $T_{\text{max}}$ | Maximum surface temperature |
| $T_{\text{friction}}$ | Temperature rise caused by frictional work |
| $T_{\text{plastic}}$ | Temperature rise caused by plastic work |
| $U$ | Rolling velocity, $(U_1 + U_2)/2$ |
| $U_c$ | Lubricant entrainment velocity |
| $v$ | Sliding velocity, $U_1 - U_2$ |
| $V_{\text{oil}}$ | Lubricant viscosity |
| $w$ | Parameter in the Fourier transform equations, $-i\omega P_e$ |
| $W_o$ | Load-dependent parameter of the oil film thickness |
| $\phi$ | Normalized temperature, $\frac{Tk}{2a_0}$ |
| $\phi_o$ | Fourier-transformed temperature, $\int_{-\infty}^{\infty} \phi(\psi)\exp(i\omega\psi)\,d\psi$ |
| $\phi_T$ | Fourier transform coefficient |
| $\eta$ | Normalized time, $\frac{t}{t_0}$ |
| $\alpha$ | Normalized depth, $\frac{z}{2a}$ |
| $\beta$ | Partition coefficient of the frictional heat |
| $\gamma$ | Plastic shear strain |
| $\gamma_0$ | Fitting value for the material properties dependent on the initial plastic strain |
| $\rho$ | Density of the disk |
| $\rho_{\text{oil}}$ | Lubricant density |
| $\mu$ | Friction coefficient |
| $\lambda$ | Lambda ratio |
| $\tau$ | Shear stress |
| $\tau_s$ | Shear stress on the contact surface |
| $\tau_0$ | Material property parameter dependent on the shear strength |
| $\omega$ | Rotating velocity of the disk |
| $\theta$ | Heat generation rate induced by the plastic work |
| $\phi_T$ | Fourier-transformed temperature, $\int_{-\infty}^{\infty} \phi(\psi)\exp(i\omega\psi)\,d\psi$ |
| $\phi_o$ | Fourier transform coefficient |
| $\eta$ | Normalized time, $\frac{t}{t_0}$ |
| $\alpha$ | Normalized depth, $\frac{z}{2a}$ |
| $\beta$ | Partition coefficient of the frictional heat |
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| $\theta$ | Heat generation rate induced by the plastic work |

### Subscripts

- $j = 1, 2, 3$  Gauss–Hermite integration numbers
subsurface layers generates a high temperature rise, leading to material softening, while plastic dislocations result in material hardening. Thus, scuffing occurs when the softening effect of the material overcomes the hardening effect [22]. This theory postulates that the high temperature during scuffing is induced by plastic work in subsurface shear bands. However, thermal effects during scuffing result from the effects of both frictional heat and plastic work [7, 8]. The frictional heat source on the contact surfaces generates a high temperature rise, and the resultant material softening at high temperatures triggers plastic deformation in the subsurface layers. Then, the plastic work is a volumetric heat source that generates a further temperature rise until shear instability becomes imminent and scuffing occurs.

Although scuffing damage involves complex physical and chemical processes [23–25], the present study mainly focuses on the transient thermal effects and material reactions during scuffing. Herein, the thermal shock on the subsurface material of M50 steel during the scuffing process is investigated. The temperature rise due to the effects of both frictional heat and plastic work is calculated using the Fourier transform method considering temperature-dependent material properties. Based on this analysis, the material damage behavior of M50 steel during scuffing is clarified.

2 Scuffing tests

A rolling–sliding contact test rig was used for the scuffing tests (Fig. 1(a)). The two-disk M50 steel tribo-parts formed an elliptical point contact. The lubricant (No. 4050 oil, 350 K) was sprayed onto the contact area at 1.053 L/min. Hydraulic step loads were applied to accurately detect scuffing failure. The rapid rises in the disk bulk temperature and spindle power consumption were considered as the indicators of scuffing (Fig. 1(b)). Table 1 lists the test parameters, and Table 2 lists the composition of the M50 steel. The sliding velocity (v) and rolling velocity (U) are defined as v = U1 − U2 and U = (U1 + U2)/2, where U1 and U2 are the surface velocities of disks 1 and 2, respectively. The damage behavior of the M50 steel after scuffing was examined via the field-emission scanning electron microscope (FESEM; Quanta 200F, FEI company, USA), the three-dimensional computed tomograph (3D-CT; Zeiss Xradia 520 Versa, Carl Zeiss, Germany), the transmission electron microscope (TEM), and the X-ray diffraction (XRD). To estimate the lubrication status during scuffing, the minimum thickness of the lubricant film is determined by [26]:

\[ D_{\text{min}} = 3.68U_{e}^{0.68}G_{e}^{0.49}W_{e}^{0.073} (1 - \exp(-0.67k_{e})) \]  

where \( U_{e}, G_{e}, \) and \( W_{e} \) are dimensionless parameters corresponding to the speed, material, and load, respectively, and \( k_{e} \) is the ellipticity ratio.

3 Fourier-transformed solution of the surface temperature during scuffing

During scuffing of the M50 steel, a high specific energy is generated by both frictional and plastic work (Fig. 2). The frictional power intensity (\( q \)) on the contact surface is

\[ q = \begin{cases} \alpha \mu p_{\text{max}} v \left[ 1 - \left(1 - \frac{2}{t_{0}}\right)^{2} \right], & t \leq t_{0} \\ 0, & t > t_{0} \end{cases} \]  

where \( \mu \) is the friction coefficient, \( p_{\text{max}} \) is the maximum
Table 1  Scuffing test parameters for the M50 steel.

| Parameter                              | Symbol | Unit  | Value  |
|----------------------------------------|--------|-------|--------|
| Sliding velocity                       | v      | m·s\(^{-1}\) | 7.42   |
| Rolling velocity                       | U      | m·s\(^{-1}\) | 49.52  |
| Radius of disk 1 in rolling direction  | R\(_1\) | mm    | 63.5   |
| Radius of disk 1 in axial direction    | r      | mm    | 20     |
| Radius of disk 2 in rolling direction  | R\(_2\) | mm    | 36.5   |
| Surface roughness of disk 1            | R\(_{a1}\) | μm  | 0.113  |
| Surface roughness of disk 2            | R\(_{a2}\) | μm  | 0.035  |
| Rotating velocity of disk 1            | \(\omega_1\) | rpm | 8,000  |
| Rotating velocity of disk 2            | \(\omega_2\) | rpm | 12,000 |
| Surface velocity of disk 1             | U\(_1\) | m·s\(^{-1}\) | 53.23  |
| Surface velocity of disk 2             | U\(_2\) | m·s\(^{-1}\) | 45.81  |
| Lubricant entrainment velocity         | U\(_e\) | m·s\(^{-1}\) | 49.52  |
| Lubricant density                      | \(\rho_{oil}\) | kg·m\(^{-3}\) | 907.4  |
| Lubricant viscosity                    | V\(_{oil}\) | Pa·s | 7.5×10\(^{-3}\) |
| Lubricant viscosity pressure coefficient | C\(_{VP}\) | Pa\(^{-1}\) | 1.23×10\(^{-8}\) |
| Lubricant viscosity temperature coefficient | C\(_{VT}\) | K\(^{-1}\) | 0.029   |
| Minimum lubricant film thickness       | D\(_{min}\) | μm | 0.16    |
| Lambda ratio                           | \(\lambda\) | — | 1.36    |
| Maximum bulk temperature during scuffing | T\(_b\) | K   | 407     |
| Hertzian contact pressure at scuffing  | \(p_{scuffing}\) | GPa | 3.3     |

Table 2  Composition of the M50 steel.

| Element | C | Cr | Mo | Si | Mn | V | W | Fe |
|---------|---|----|----|----|----|---|---|----|
| wt%     | 0.84 | 3.97 | 4.37 | 0.21 | 0.15 | 1.64 | 0.25 | Balance |

Fig. 2  Schematic of the high specific energy during scuffing caused by both frictional and plastic work.

contact pressure, \(t_0\) is the contact time \((2a/U)\), \(a\) is the contact radius, and \(\alpha\) is the partition coefficient of the frictional heat.

According to the Tian–Kennedy model [13], the maximum surface temperature \((T_{max})\) is

\[
T_{max} = 0.773 \frac{2\alpha q}{k\pi p_e} = \frac{2.186\sqrt{\alpha q}}{\sqrt{\pi\rho c kU}} \tag{3}
\]

where \(k\), \(\rho\), and \(c\) are the heat conduction, density, and specific heat capacity of the material, respectively. By assuming an identical maximum temperature on the two contact surfaces composed of the same material, the partition coefficient of the frictional heat on the contact surface of disk 1 is

\[
\alpha = \frac{\sqrt{U_1}}{\sqrt{U_1 + U_2}} \tag{4}
\]

The frictional power intensity on contact surface of disks 1 and 2 are given by

\[
\begin{align*}
q_1 &= \alpha q \\
q_2 &= (1 - \alpha) q \tag{5}
\end{align*}
\]

where \(q\) is the frictional power intensity.

For the plastic flow in the subsurface layers during scuffing, the relationship between the shear stress and strain of the M50 steel is

\[
\tau = \begin{cases} 
\tau_0 \left( \frac{m + \gamma}{\gamma_0} \right)^n, & 0 \leq \gamma \leq 0.02 \\
\tau_0 \left( \frac{m + 0.02}{\gamma_0} \right)^n + E_T (\gamma - 0.02), & \gamma > 0.02 
\end{cases} \tag{6}
\]

where \(\tau\) is the shear stress, and \(\gamma\) is the plastic shear strain. For the M50 steel, the work hardening coefficient \(n\) is 0.095, and the fitting values \(m\) and \(\gamma_0\) are 4 and 10\(^{-6}\), respectively. \(\tau_0\) is dependent on the shear strength, and \(E_T\) is the tangential modulus. The temperature-dependent relationship between the shear stress and strain is shown in Fig. 3.

Fig. 3  Temperature-dependent relationship between the shear stress and strain of the M50 steel.
The normalized temperature, $\theta$, is the inverse Fourier transform of $\varphi$:

$$
\theta(\tau) = \frac{1}{2\pi} \int_{-\infty}^{\infty} \varphi(\psi) e^{i\psi \tau} d\psi,
$$

where

$$
\varphi(\psi) = \frac{1}{2\pi} \int_{-\infty}^{\infty} \theta(\tau) e^{-i\psi \tau} d\tau.
$$

The Fourier transform of the strain energy density is

$$
\phi_s(\eta) = \int_{-\infty}^{\infty} \phi(\psi) e^{i\psi \eta} d\psi,
$$

and its inverse is

$$
\phi(\psi) = \frac{1}{2\pi} \int_{-\infty}^{\infty} \phi_s(\eta) e^{-i\psi \eta} d\eta.
$$

The Fourier transform rules in Eq. (11) are applied:

$$
\phi_s(0) = F\{\phi(\psi)\} = \frac{1}{2\pi} \int_{-\infty}^{\infty} \phi(\psi) e^{i\psi \eta} d\psi
$$

The general solution for Eqs. (12a) and (12b) is

$$
\frac{\partial^2 \phi}{\partial \eta^2} = \frac{F}{i\eta} \phi_s(\eta),
$$

where $s$ is the Fourier transform of the normalized shear stress, and the coefficients $w, M_s, A_s, B_s$, and $C_s$ are defined as Eq. (12c):

$$
w = -i\eta \phi_s(\eta),
$$

$$
A_s = \frac{K_s}{4P_s},
$$

$$
B_s = \frac{K_s}{4P_s},
$$

$$
C_s = \frac{K_s}{4P_s}.
$$
the Fourier-transformed temperature at $s_{r_0} \leq s \leq s_{r_0 + 0.02}$ is obtained as

$$
\phi_s(\eta, \vartheta) = \frac{Q_s(\vartheta)}{\sqrt{w}} \exp\left(-\sqrt{w}\eta\right) - \frac{M_{s1}^{s1/\psi+1}(\vartheta)}{(H^2 w - 1)H^{\psi}} \exp\left(-\sqrt{w}\eta\right)
+ \frac{H^{2 \psi} M_{s1}^{s1/\psi+1}(\vartheta)}{H^{2 \psi} - 1} \exp\left(-\eta/H\right)
$$

(15a)

If $s > s_{r_0 + 0.02}$, the Fourier-transformed temperature is

$$
\phi_s(\eta, \vartheta) = \frac{Q_s(\vartheta)}{\sqrt{w}} \exp\left(-\sqrt{w}\eta\right) - \frac{M_{s1}^{s1/\psi+1}(\vartheta)}{(H^2 w - 1)H^{\psi}} \exp\left(-\sqrt{w}\eta\right)
+ \frac{H^{2 \psi} M_{s1}^{s1/\psi+1}(\vartheta)}{H^{2 \psi} - 1} \exp\left(-\eta/H\right)
$$

(15b)

where $Q_*$ is the normalized frictional power intensity ($q/q_0$).

Using the inverse Fourier transform, the normalized temperature at $s_{r_0} \leq s \leq s_{r_0 + 0.02}$ is

$$
\phi(\eta, \vartheta) = \int_{-\infty}^{\infty} \phi_s(\eta, \theta) \exp(-i\vartheta \theta) \, d\theta
= \frac{1}{2\pi} \int_{-\infty}^{\infty} \frac{Q_s(\vartheta)}{\sqrt{w}} \exp\left(-\sqrt{w}\eta\right) \exp(-i\vartheta \theta) \, d\theta
- \frac{1}{2\pi} \int_{-\infty}^{\infty} \frac{M_{s1}^{s1/\psi+1}(\vartheta)}{(H^2 w - 1)H^{\psi}} \exp\left(-\sqrt{w}\eta\right) \exp(-i\vartheta \theta) \, d\theta
+ \frac{H^{2 \psi} M_{s1}^{s1/\psi+1}(\vartheta)}{H^{2 \psi} - 1} \exp\left(-\eta/H\right) \exp(-i\vartheta \theta) \, d\theta
$$

(16a)

If $s > s_{r_0 + 0.02}$, the normalized temperature is

$$
\phi(\eta, \vartheta) = \frac{1}{\sqrt{4\pi P^e}} \int_{\psi - \psi'}^{\psi} \frac{Q^*(\psi')}{\sqrt{\psi - \psi'}} \exp\left(-\eta^2 P^e/\psi - \psi'\right) \, d\psi'
+ \frac{1}{\pi \sqrt{4P^e}} \int_{\psi - \psi'}^{\psi} M_{s2}^{s2/\psi+1}(\psi') \exp\left(-\eta^2 P^e/\psi - \psi'\right) \, d\psi'
\cdot \sum G_j(\eta, \psi')
$$

(16b)

where $\psi'$ is the integration variable of the normalized contact time, $G_j(\eta, \psi')$ is an integration function solved using the Gauss–Hermite method:

$$
G_j(\eta, \psi') = \frac{C, 4P^e H\eta \sqrt{\psi - \psi'} + 2(\psi - \psi')^{3/2}}{H(\psi - \psi')^2 + 4P^e H^2 (B^2/H + \eta)(\psi - \psi') + 4P^2 H^3 \eta^2}
$$

(17)

where coefficients $B_j$ and $C_j$ are listed in Table 3.

Substituting $\chi = \sqrt{\psi - \psi'}$ into Eqs. (16a) and (16b), the normalized temperature at $s_{r_0} \leq s \leq s_{r_0 + 0.02}$ is

$$
\phi(\eta, \psi) = \frac{1}{\sqrt{4\pi P^e}} \int_{0}^{\sqrt{\psi - \psi'}} Q^*(\psi - \chi^2) \exp\left(-\eta^2 P^e/\chi^2\right) \, d\chi
+ \frac{1}{\pi \sqrt{4P^e}} \int_{0}^{\sqrt{\psi - \psi'}} M_{s2}^{s2/\psi+1}(\psi - \chi^2) \exp\left(-\eta^2 P^e/\chi^2\right) \sum G_j(\eta, \chi) \, d\chi
$$

(18a)

If $s > s_{r_0 + 0.02}$, the normalized temperature is

$$
\phi(\eta, \psi) = \frac{1}{\sqrt{4P^e}} \int_{\psi - \psi'}^{\psi} \frac{Q^*(\psi')}{\sqrt{\psi - \psi'}} \exp\left(-\eta^2 P^e/\psi - \psi'\right) \, d\psi'
+ \frac{1}{\pi \sqrt{4P^e}} \int_{\psi - \psi'}^{\psi} \sum G_j(\eta, \psi') \, d\psi'
$$

(18b)

where the integration function $G_j(\eta, \chi)$ is

$$
G_j(\eta, \chi) = \frac{C, 4P^e H\eta \chi^2 + 2\chi^4}{H\chi^4 + 4P^e H^2 (B^2/H + \eta)\chi^2 + 4P^2 H^3 \eta^2}
$$

(19)

Along the centerline of the Hertz elliptical contact surface, the normalized frictional heat flux and tangential stress are respectively as Eq. (20):

$$
Q^*(\psi - \chi^2) = s(\psi - \chi^2)
= \begin{cases} 
2\sqrt{\psi - \chi^2 - \psi^2 + 2\psi\chi^2 - \chi^4}, & 0 \leq \psi \leq 1 \\
0, & \psi > 1 
\end{cases}
$$

(20)

4 Results and discussion

The damage behavior of the subsurface material of the M50 steel after scuffing was examined. The FESEM was used to examine the damage morphologies of steels during scuffing, and 3D-CT was used to examine the cracks in the material. The size of the samples was 02 mm × 10 mm, and the 3D-CT micrographs were
imaged at a voltage of 100 kV using voxels with a side length of 70 nm. SEM and 3D-CT images of the surface failure of the M50 steel are shown in Fig. 4. Loss of surface integrity occurred in the contact areas with local micro-welding and severe plastic deformation (Fig. 4(a)). No significant cracks were observed in the subsurface material (Fig. 4(b)). Therefore, the adhesion of the two contact surfaces led only to scuffing damage rather than fatigue failure.

To observe the plastic deformation in the subsurface material, cross sections of the samples were cut using electrical discharge machining. The cross sections were then polished and etched using an alcoholic solution containing 5% nitric acid. FESEM observations were conducted on the cross sections at an accelerating voltage of 10 kV. Figure 5 shows shear bands in the local subsurface regions of the M50 steel due to scuffing. Severe plastic flow occurred at a depth of approximately 15 μm in the subsurface layers (Fig. 5(a)). Furthermore, TEM images of the damage layers indicate that extensive shear slip lines formed in local regions, as indicated by the red arrows in the red box (Fig. 5(b)). Shear slip occurred in the austenite phase of the M50 steel at high temperatures. During the subsequent martensitic transformation process, the shear bands in austenite acted as grain boundaries, thereby limiting the nucleation and growth of shear martensite. Owing to this process, most of the martensite laths were parallel to the shear bands. These observations of the scuffing damage in subsurface layers provide compelling evidence for shear instability with a high temperature and severe plastic flow.

Figure 6 shows the changes in the XRD spectra of the M50 steel due to scuffing. The spectra reveal a (111) diffraction peak corresponding to retained austenite, which is consistent with the presence of metastable austenite in the damage region of the M50 steel. However, only martensitic diffraction peaks are observed in the spectrum of the original M50 steel. The absence of the austenite peaks in the spectrum of the original M50 steel is due to the tempering performed three times at 830 K for 1 h, which led to the decomposition of the austenite. Therefore, the retained austenite validates that austenitization and quenching occurred during the scuffing process of the M50 steel.

The surface temperature rise and plastic strain at the high-speed rolling–sliding contacts were analyzed (Fig. 7), where $T_{\text{friction}}$ and $T_{\text{plastic}}$ represent the temperature rise caused by frictional and plastic work, respectively. The contact parameters were the same

### Table 3 Coefficients for Gauss–Hermite integration.

| Coefficient | $B_1$     | $B_2$     | $B_3$     | $C_1$     | $C_2$     | $C_3$     |
|-------------|-----------|-----------|-----------|-----------|-----------|-----------|
| Value       | 2.0201828705 | 0.9585724646 | 0         | 0.0199532421 | 0.3936193232 | 0.9453087205 |

Figures:
- Fig. 4 Material damage of the M50 steel due to scuffing: (a) SEM micrograph of the worn surface and (b) 3D-CT image of the damage region, which display no noticeable cracks.
- Fig. 5 Shear bands in local subsurface layers of the M50 steel due to scuffing: (a) SEM image of the plastic flow and (b) TEM image of the shear band microstructure.
- Fig. 6 XRD spectra of the M50 steel.
as the scuffing test parameters (Table 1). The friction coefficient was \(\mu = 0.15\), and the contact time was \(t_0 = 37 \mu s\). A sharp rise and subsequent rapid decrease in the surface temperature were observed with a maximum \(T_{\text{friction}} + T_{\text{plastic}} = 1,240\) K (Fig. 7(a)). The maximum surface temperature rise caused by the frictional heat was \(T_{\text{friction}} = 860\) K at \(t = 0.83t_0\). The plastic work generated a maximum surface temperature rise \(T_{\text{plastic}} = 435\) K at \(t = 0.65t_0\). However, at \(t = 10t_0\) the temperatures decreased to \(T_{\text{friction}} = 140\) K and \(T_{\text{plastic}} = 40\) K. A rapid temperature change during a short time is defined as a thermal shock.

The thermal shock effects reasonably account for the scuffing-damage behavior of the M50 steel. During contact, the rapid increase in the contact temperature led to strong material softening, that is, a low shear strength of the M50 steel (Fig. 7(b)). Consequently, plastic deformation was generated by the tractive stress at the contacts (Figs. 7(b) and 5(a)), leading to a further temperature increase (Fig. 7(a)). Moreover, the high temperature (> 1,025 K) resulted in austenitization of the M50 steel, while the rapid decrease in the temperature led to the quenching of the M50 steel, which caused the shear slip bands (Fig. 5(b)) and retained austenite (Fig. 6) in the M50 steel after scuffing.

In addition, when the contact time is short, the heat penetration depth is small (Fig. 7(c)). Consequently, the depth of the plastic deformed layers was only approximately 20 \(\mu m\) (Fig. 7(d)).

Figure 8 shows the surface temperature rise of the M50 steel at rolling–sliding contacts with different frictional coefficients. Increasing the frictional coefficient caused a linear rise in \(T_{\text{friction}}\). Plastic deformation occurred when the frictional coefficient was larger than 0.125 because a large frictional coefficient generated both a high tractive stress on the contact surface and a low shear strength due to the high temperature. The plastic work led to a further temperature rise in \(T_{\text{plastic}}\).

Figure 9 shows the surface temperature rise of the M50 steel under different contact pressures; the friction coefficient was \(\mu = 0.15\). A high contact pressure is accompanied by a large contact radius \((a)\) and a long contact time \((t_0)\). Therefore, increasing the contact pressure resulted in a more rapid increase in \(T_{\text{friction}}\). Because of both the high tractive stress and thermal softening effects, plastic deformation occurred under contact pressures higher than 3.0 GPa.

Figure 10 shows the surface temperature rise of the M50 steel as a function of the sliding velocity; the friction coefficient was \(\mu = 0.15\), and the rolling velocity

![Fig. 7](image-url) Thermal shock of the subsurface material during scuffing of the M50 steel: (a) rapid change in surface temperature due to the effects of frictional and plastic work, (b) plastic strain and tractive stress on the contact surfaces during contact, and variation in (c) temperature and (d) plastic strain with depth.
was $U = 49.52$ m/s. Increasing the sliding velocity increased the frictional power intensity, leading to an increase in the temperature rise $T_{\text{friction}}$. Plastic deformation occurred when the sliding velocity was higher than 6 m/s because of thermal softening effects. However, the plastic strain increased slowly at a high sliding velocity (Fig. 10(b)), because the shear strength decreased slowly at high contact temperatures (Fig. 7(b)).

Figure 11 shows the effect of the rolling velocity on the surface temperature rise of the M50 steel; the friction coefficient was $\mu = 0.15$, and the sliding velocity was $v = 7.42$ m/s. A high rolling velocity led to a short contact time. Consequently, the contact temperature decreased nonlinearly with increasing rolling velocity, resulting in a decrease in plastic
deformation. When the rolling velocity was higher than 70 m/s, the plastic deformation did not occur (Fig. 11(b)).

5 Conclusions

The surface damage behavior of M50 steel during scuffing was investigated, and the effect of thermal shock during the scuffing process was analyzed considering the effects of both frictional and plastic work. The main conclusions are as follows:

1) Thermal shock with a rapid rise and reduction in the contact temperature is one of the root causes of the scuffing damage of the M50 steel. Under the test conditions, a rapid rise in the temperature to over 1,050 K for approximately 0.02 ms led to austenitization of the M50 steel. Shear instability with severe plastic flow was generated in the subsurface layers. The subsequent rapid decrease in the contact temperature over approximately 0.4 ms led to quenching of the M50 steel. Consequently, shear slip bands and retained austenite were generated.

2) The rise in contact temperature is a strong function of the frictional power intensity. Increasing the friction coefficient caused a linear increase in the contact temperature, and increasing the contact pressure led to a more rapid increase in the contact pressure because of the larger contact radius. However, a high rolling velocity resulted in a low contact temperature owing to the short contact time.

3) Owing to the decrease in the shear strength of the subsurface material at high temperatures, plastic flow was generated by the high tractive stress, and the resultant plastic strain energy generated a further temperature increase. Because the heat penetration depth was small for short contact time, the depth of the subsurface layers with noticeable plastic flow was only approximately 15 μm under the test conditions.

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