Study on temperature of cylindrical wet grinding considering lubrication effect of grinding fluid

Yong Zheng1 · Changqing Wang1,2 · Yifei Zhang1 · Fanming Meng1

Received: 15 March 2022 / Accepted: 3 July 2022 / Published online: 22 July 2022
© The Author(s), under exclusive licence to Springer-Verlag London Ltd., part of Springer Nature 2022

Abstract

The improved cylindrical wet grinding temperature (ICWGT) model considering the lubrication effect of the grinding fluid is established and solved numerically. In doing so, the Newton–Raphson method is used to solve the hydrodynamic pressure of grinding fluid, and the fast Fourier transform (FFT) method is employed to accelerate the deformation calculation. Furthermore, the convective heat transfer coefficient (CHTC) is calculated based on the heat transfer theory, and the moving heat source method is adopted to obtain the grinding temperature. Meanwhile, the effectiveness of ICWGT is verified through the grinding temperature measurement experiment of alloy steel. Then, the cylindrical wet grinding temperature for alloy steel considering the lubrication effect of grinding fluid is studied and compared at varied grinding process parameter, and some rules are revealed.

Keywords  Cylindrical wet grinding · Lubrication effect · CHTC · Grinding temperature experiment

Nomenclature

\( a_{\text{max}} \) Undeformed chip thickness (mm)
\( B_w, B_t \) Widths of workpiece and wheel (mm)
\( c_p, c_r, c_w \) Specific heat capacities of grinding fluid, wheel, and workpiece (J/(kg·K))
\( C \) Ratio of the chip width to chip thickness
\( d_g \) Equivalent diameter of abrasive grain (mm)
\( D_w, D_t \) Diameters of wheel and workpiece (mm)
\( E_w, E_t \) Young’s moduli of wheel and workpiece (GPa)
\( f_r \) Radial infeed rate of wheel (mm/min)
\( F_n, F_t \) Normal grinding force and tangential grinding force (N)
\( h, h_c \) Local and central grinding fluid thicknesses (μm)
\( h_p, h_w \) Convection heat transfer coefficients of grinding fluid and workpiece (W/(m²·K))
\( i, j, k \) Discrete position indexes along x-, y- and z-directions
\( k_p, k_r, k_w \) Thermal conductivities of grinding fluid, wheel, and workpiece (W/(m·K))
\( l_c \) Real contact arc length (mm)
\( n_w \) Speed of workpiece (r/min)
\( N_d \) Active grits number per unit area
\( N_x, N_y \) Numbers of grid nodes along x- and y-directions
\( O-\text{xyz} \) Global coordinate system
\( p \) Hydrodynamic pressure of grinding fluid (MPa)
\( q_{ch}, q_f \) Heat flux into the chips and grinding fluid (W/mm²)
\( q_s, q_w \) Heat flux into the grinding wheel and workpiece (W/mm²)
\( r_0 \) Effective contact radius of grains (mm)
\( R_r \) Roughness factor of wheel
\( R_{w}, R_{w1}, R_{w2} \) Equivalent curvature radii of workpiece and grinding wheel along the x- and y-directions (mm)
\( R_w \) Heat partition ratio of workpiece
\( R_{p,w} \) Workpiece-chip partition ratio and workpiece-wheel partition ratio
\( u_e \) Entrainment velocity of grinding fluid (m/s)
\( U \) Velocity of grinding fluid along x-direction (m/s)
\( v \) Total elastic deformation
\( U_a \) Average speed of grinding fluid (m/s)
\( V_s \) Speed of grinding wheel (m/s)

© Fanming Meng
fmmeng@cqu.edu.cn

1 The State Key Laboratory of Mechanical Transmission, Chongqing University, Chongqing 400044, China
2 Harbin Dong’an Engine Co., Ltd., Harbin 150066, Heilongjiang, China
Grinding is an important process to ensure the surface quality of workpiece, in which high grinding temperature is often accompanied [1–3]. In order to avoid the occurrence of grinding burn caused by the grinding temperature rise, grinding fluid is often used to take away the grinding heat, which is referred to as the wet grinding. To investigate the wet grinding temperature, some theoretical and experimental researches have been conducted to date. Desruisseaux and Zerkle [4] studied the cylindrical wet grinding temperature using the uniformly distributed heat source theory and pointed out that the grinding temperature is affected by the cooling effect of the grinding fluid. Kim et al. [5] investigated the heat flux distribution and energy partition considering the convective heat transfer of grinding fluid and found that the triangular heat flow is suitable for the experimental analysis. Further, Li et al. [6, 7] studied the actual wet grinding flux distribution in the cylindrical wet grinding process by using the inverse heat transfer analysis of the measured temperature distribution and observed that a quadratic curve heat source distributed model was more realistic in estimating the workpiece grinding temperature. An inappropriate convective heat transfer coefficient (CHTC) employed in the above researches, however, brings out large calculation error in assessing the cooling effect of the grinding fluid. To improve the calculation accuracy, Lavine [8] proposed an average convection coefficient to evaluate the cooling effect of grinding fluid. Miao et al. [9] further used the average convection coefficient to calculate the temperature field of turbine blade root. Rowe et al. [10] developed a grinding fluid wheel model (FWM), in which the fluid layer was traveled at wheel speed to estimate the CHTC of the grinding fluid. Zhang et al. [11] proposed a laminar flow model (LFM) based on fluid dynamics and heat transfer to calculate the CHTC of the grinding fluid. Furthermore, Lin et al. [12] proposed an improved fluid heat transfer model, in which the average speed between the workpiece and grinding wheel was used to describe the speed of the fluid.

Except for the above theory and method employed to evaluate the cooling effect of grinding fluid, the lubrication performance of grinding fluid has been studied by few researchers. Chang [13] investigated the useful flow rate of grinding fluid by solving modified Reynolds equation and found that the porosity of workpiece is closely related to the hydrodynamic pressure of grinding fluid in the contact zone. Using the hydrodynamic lubrication model, Jin et al. [14] found the CHTC is sensitive to the coolant supply pressure. Further, Li et al. [15] studied the grinding fluid optimization supply based on lubrication theory. In addition, the Reynolds equation was employed to investigate the fluid flow under smooth and rough grinding conditions [16, 17]. The above researches, however, focus only on the lubrication pressure of grinding fluid but ignore the workpiece deformation and CHTC caused by the pressure of the grinding fluid, which played an important role in the grinding thermal analysis. Besides, when the grinding fluid enters the gap between the grinding wheel and the workpiece, the grinding fluid thickness decreases due to the hydrodynamic action. Meanwhile, the enhanced grinding pressure solved by the Navier–Stokes equation is often accompanied by the occurrence of negative pressure, which will further affect the grinding temperature.

In the present work, taking an alloy steel bearing track (i.e., workpiece) as an example, the improved cylindrical wet grinding temperature (ICWGT) model is established and numerically solved to obtain the CHTC considering the lubrication effect of the grinding fluid and the normal deformation of the workpiece. Meanwhile, a fast Fourier transform (FFT) method, [18–20] due to its good computation efficiency and accuracy, is adopted to accelerate the calculation of the elastic deformation of the steel track. Then, the experiment of cylindrical wet grinding is carried out to verify the improved grinding temperature model. Finally, the associated conclusions are drawn.

### 2 Improved temperature calculation model

#### 2.1 Governing equations

A cylindrical wet grinding model for alloy steel bearing track composed of bearing track, grinding wheel, and nozzle is shown in Fig. 1a, in which symbols x, y, and z denote the flowing direction of the grinding fluid, width direction of the grinding wheel, and depth direction of the bearing track, respectively. The width and diameter of the grinding wheel are separately denoted by $B_p$ and
$D_s$ and the diameter of bearing track is denoted by $D_w$. When the grinding wheel rotates counterclockwise at the speed $v_s$ and feeds radially at the infeed rate $f_r$, and the bearing track rotates clockwise at speed $n_w$, there exists the tangential grinding force $F_t$ along the $x$-direction and normal grinding force $F_n$ along the $z$-direction. Thus, the total heat flux $q_t$ due to the effect of $F_t$ is generated in the grinding contact zone with length $l_c$. Meanwhile, the grinding fluid from the injector which plays the role of cooling and lubrication is brought into the gap between the grinding wheel and bearing track. Affected by the compression of $F_n$ on the grinding fluid thickness $h$, as shown in Fig. 1b, the hydrodynamic action of the grinding fluid occurs and further the deformation $v$ appears on the workpiece. In the actual grinding process, a lot of grinding heat is generated at the interface between the grinding wheel and workpiece. Such high grinding heat is distributed to the workpiece, grinding wheel, grinding fluid and chips, whose corresponding values are denoted by $q_w$, $q_s$, $q_f$ and $q_c$. The grinding heat denoted by $q_w$ moves along the grinding contact zone and determines the surface temperature of the workpiece. Based on the Jaeger’s moving heat source theory [3], the temperature distribution of the workpiece can be written as

$$T(x, z) = \frac{1}{\pi k_w} \int_0^{l_c} q_w(\zeta) \exp \left[ -\frac{\pi(x - \zeta)}{120\alpha_wD_w} \right] K_0 \left[ \frac{\pi D_w n_w}{120\alpha_w} \right] \sqrt{\frac{D_w + D_s}{D_w D_s}} d\zeta + T_e$$

where symbol $k_w$ denotes the thermal conductivity of the workpiece, the thermal diffusivity of the workpiece is defined as $a_w = \sqrt{k_w/(\rho_w c_w)}$, in which $\rho_w$ and $c_w$ are separately the density and specific heat of the workpiece. $K_0$ and $T_e$ are separately the modified Bessel function of the second kind of order zero and environment temperature. In this paper, the environment temperature is set as 30 °C. The real length $l_c$ of the contacting arc can be evaluated according to the following relation [21]

$$l_c = \sqrt{\frac{8R^2F_n}{B} \left( \frac{1 - v_w^2}{\pi E_w} + \frac{1 - v_s^2}{\pi E_s} \right) D_s + \frac{f_r}{n_w} D_e}$$

where $E_w$ and $v_w$ are separately the elastic modulus and Poisson’s ratio of the workpiece, and the corresponding values of the grinding wheel are denoted by $E_s$ and $v_s$, respectively. $R_e$ denotes the roughness factor of the grinding wheel, set as 7 here [12]. The equivalent diameter of the workpiece and grinding wheel is defined as $D_e = D_w D_s/(D_w + D_s)$. In the whole process of the cylindrical wet grinding, the values of $F_t$ and $F_n$ can be expressed as [22, 23]

$$F_t = ABw^{-2k} \left( \frac{f_r}{n_w} \right)^{1-k/2} \left( \frac{\pi D_w n_w}{60v_s} \right)^{1-k} \left( \frac{D_w + D_s}{D_w D_s} \right)^{-k/2}$$

$$F_n = \frac{1}{\pi k_w} \int_0^{l_c} q_w(\zeta) \exp \left[ -\frac{\pi(x - \zeta)}{120\alpha_wD_w} \right] K_0 \left[ \frac{\pi D_w n_w}{120\alpha_w} \right] \sqrt{\frac{D_w + D_s}{D_w D_s}} d\zeta + T_e$$

Fig. 1 Cylindrical grinding model of bearing track for alloy steel: a schematic diagram of cylindrical grinding and b diagram of heat flux distribution.
Here, $\lambda$, $w$, and $k$ are separately the material correlation coefficient, grain interval of the grinding wheel, and empirical constant, which can be obtained by the iterations in Sect. 2.2. The symbol $\theta$ denotes the half apex angle of the abrasive grain and whose value is usually taken as 60°. It should be pointed out that the tangential grinding force $F_t$ and normal grinding force $F_w$ in Eq. (4) are obtained by the inverse matching between the simulated results and the experimental results, which are different from those in the dry grinding condition.

In solving Eq. (1) for the workpiece temperature, the value of $q_w$ should be obtained in advance, which can be evaluated as

$$q_w = q_R w$$  

(5)

Here, the total grinding heat flux is defined as $q_R = F_t vs / B l_c$, in which symbol $B$ denotes the effective grinding width, whose value is equal to the smaller one between the widths of grinding wheel and workpiece. The ratio of heat flowing into the workpiece is denoted by $R_w$, which can be computed according to the following relation [14]

$$R_w = \left( \frac{1}{R_{ws}} + \frac{1}{R_{wch}} - \left( \frac{h_f}{h_w} \right) \right)^{-1}$$  

(6)

In Eq. (6), the convection heat transfer coefficients of the grinding fluid and workpiece are denoted with $h_f$ and $h_w$, respectively. $R_{ws}$ and $R_{wch}$ are separately the heat partition ratios of the workpiece-wheel and workpiece-chip [24, 25], that is,

$$R_{ws} = \left( 1 + \frac{0.974 k_s}{\rho w n_v} \left( 1 - e^{-\tau} \right)^{-1} \right)^{-1}$$  

(7)

$$R_{wch} = \frac{1.328 a_w r / (v_s a_{gmax})}{1 + 1.328 a_w r / (v_s a_{gmax})}$$  

(8)

where the thermal property parameters of the workpiece is evaluated through $\beta_w = \sqrt{k_s \rho_w c_w}$. The dimensionless time $\tau = \sqrt{k_s 0.5 r_0^2} (r_0^2 n_v) (a_p c_s) 0.5$, in which $k_s$, $\rho_s$, and $c_s$ are separately the thermal conductivity, density, and specific heat of the grinding wheel. The shear strain is defined as $\gamma = \cos \theta / (\sin \phi \cos (\phi + \theta))$, in which $\theta$ and $\varphi$ are separately the half of grit tip angle and shear angle. And the relationship between the values of $\theta$ and $\varphi$ can be determined by $\varphi = (90° - \theta)/2$ [25]. $r_0$ represents the effective contact radius of the abrasive grain, which can be calculated by $r_0 = a_{gmax} \tan \theta$. Symbol $a_{gmax}$ is the maximum undeformed chip thickness, which can be defined as [25]

$$F_n = \frac{4\lambda B \tan(\theta) v_w^{-2k}}{\left( \frac{f_r}{n_w} \right)^{1-k/2}} \left( \frac{\pi D_n n_w}{60 v_s} \right)^{1-k} \left( \frac{D_s + D_w}{D_w} \right)^{-k/2}$$  

(4)

Here, $C$ represents the ratio of the chip width to chip thickness, which is estimated using $C = 4 \tan \theta$. $N_d$ is the active grit number per unit area and can be determined by the following equation [26]

$$N_d = \frac{4f}{d_s^2 \left( \frac{4\pi}{5} \right)^{2/3}}$$  

(10)

where the equivalent diameter of grits is defined as $d_s = 15.2 M^{-1}$. $M$ represents the granularity of the grinding wheel. $f$ and $\beta$ are the empirical coefficients of the abrasive grain and represent the effective grinding edge component and volume fraction, respectively. In this paper, $f$ and $\beta$ are separately taken as 0.5 and 0.25 [27].

The convection heat transfer coefficient of workpiece $h_w$ in Eq. (6) can be described as below [12]

$$h_w = \frac{3 \beta_w}{2 C_1} \sqrt{\frac{\rho w n_v D_w}{60 l_c}}$$  

(11)

Here, $C_1$ is the temperature factor varying with the Peclet number $P_e$, which is defined as $P_e = \pi n_v D_w l_c / 24000 \alpha_w$. The value of $C_1$ is fixed to 1.06 when $P_e$ is more than 10, its value is set as 0.76 when $P_e$ is less than 0.2, and $C_1 = 0.95(P_e/2 + 2\pi)^{0.5}/\pi$ when $P_e$ is taken to be other values.

When only the cooling effect of the grinding fluid is considered, the variable convection of heat transfer coefficient $h_f$ of the fluid can be written as [13, 28]

$$h_f = \frac{B_f}{1.06} \sqrt{\frac{v_s}{l_c}}$$  

(12)

Here, the thermal property parameters $\beta_f$ of grinding fluid can be calculated by $\beta_f = \sqrt{k_f \rho_f c_f}$. Here, $K_p$, $\rho_f$, and $c_f$ are separately the thermal conductivity, density, and specific heat of the grinding fluid.

When the lubrication effect of the grinding fluid is taken into consideration, the velocity of grinding fluid is characterized by its average speed and is no longer the grinding wheel speed shown in Eq. (12). Based on the above assumption and theory of conduction of heat in solid, the convection heat transfer coefficient $h_f$ applied to the grinding fluid is converted as

$$h_f = 0.667 \rho_f c_f^{1/3} \eta_f^{-1/6} l_c^{-1/2} (U_g)^{1/2}$$  

(13)

Here, $\rho_f$, $c_f$, and $\eta_f$ are separately the density, specific heat, and dynamic viscosity of the grinding fluid. The average velocity $U_g$ of the grinding fluid computed according to hydrodynamic theory, is written as [29]
\[ U = v_y + \frac{1}{2 \eta_f} \frac{\partial p(x, y)}{\partial x} (\epsilon^2 - \epsilon h(x, y)) + \left( \frac{\pi n \delta w}{60} - v_s \right) \frac{z}{h(x, y)} \]  
(14)

\[ U_a = \frac{1}{V} \iiint_U \frac{U dx dy dz}{V} \]  
(15)

Here, \( V \) is the volume of the grinding contact zone. The pressure and thickness of the grinding fluid at any point \((x, y, z)\) in the grinding zone are denoted with \(p(x, y)\) and \(h(x, y)\), respectively.

The pressure of the grinding lubrication domain between the grinding wheel and the workpiece can be obtained by solving the following Reynolds equation

\[ \frac{\partial}{\partial x} \left( \frac{\rho_l h^3}{\eta_f} \frac{\partial p}{\partial x} \right) + \frac{\partial}{\partial y} \left( \frac{\rho_l h^3}{\eta_f} \frac{\partial p}{\partial y} \right) = 12 \mu_c \frac{\partial (\rho_l h)}{\partial x} \]  
(16)

where the entrainment velocity \(u_e = (v_y + \pi D_s n_s / 60)/2\). This equation is solved with an under-relaxation algorithm and the pressure boundary condition is employed as

\[ p(x, y) = 0, x, y \in \text{inlet or outlet} \]  
(17)

In addition, the grinding fluid thickness between the workpiece and wheel at point \((x, y)\) is known as

\[ h(x, y) = h_c + \frac{x^2}{2R_x} + \frac{y^2}{2R_y} + v(x, y) \]  
(18)

Here, \( h_c \) is the central thickness between the workpiece and grinding wheel. \( R_x \) and \( R_y \) are separately the equivalent curvature radii of the workpiece and grinding wheel along the \( x \)- and \( y \)-directions. \( v(x, y) \) is the composite elastic deformation of the workpiece and grinding wheel, which can be obtained by solving the following Boussinesq integral [30–32]

\[ v(x, y) = \frac{2}{\pi E_c} \iint_{\Omega} \frac{p(x', y')}{(x' - x)^2 + (y' - y)^2} dx' dy' \]  
(19)

where \( E_c \) is the composite elastic modulus for the workpiece and grinding wheel, defined as \( 2/E_c = \left( 1 - \nu''^2 \right)/E_w + \left( 1 - \nu''^2 \right)/E_r \).

The initial pressure of the grinding fluid is set according to the Hertz contact theory. Furthermore, the composite elastic deformation of the grinding wheel and workpiece is calculated by using the obtained initial pressure according to Eq. (19). During this process, the fast Fourier transform (FFT) method is adopted to accelerate the deformation calculation [18–20].

### 2.2 Numerical solution

In order to solve the ICWGT model of workpiece for alloy steel, the above governing equations are solved with a finite difference method. A solution process based on moving heat source method that considering the lubrication effect of the grinding fluid is proposed in the present study. The overall solution process includes the heat flux calculation denoted by the blue line and pressure iteration denoted by the red line, as shown in Fig. 2. The details of the numerical solution are as follows.

Step 1: Give the working condition parameters, dimension parameters of the workpiece and grinding wheel, and lubrication parameters of the grinding fluid. The value \( \lambda \) of material correlation coefficient, grain interval \( w \) of grinding wheel, and empirical constant \( k \) of the workpiece are set as 170 kg/mm, 0.53 mm, and 0.426, respectively.

Step 2: Use the above parameters to obtain the normal grinding force \( F_n \) and tangential force \( F_t \) according to Eqs. (3) and (4).

Step 3: Calculate the real length \( l_c \) of contact arc and convection heat transfer coefficient \( h_w \) of workpiece according to Eqs. (2) and (11), respectively.

Step 4: Solve Eq. (16) for the grinding fluid pressure \( p \). The number of grid node for the two directions is set as 128 × 128 in solving the Reynolds equation for the grinding fluid pressure. The pressure convergence accuracy \( \varepsilon_p \) and load convergence accuracy \( \varepsilon_F \) are separately set as 1.0 × 10\(^{-4}\) and 1.0 × 10\(^{-3}\) in the present study. Once the pressure and load convergence accuracy are satisfied, the pressure and load iterations are terminated. Otherwise, the initial grinding fluid pressure and center thickness \( h_c \) are separately updated according to the following relation:

\[ p_{(i,j)}^{\text{new}} = p_{(i,j)}^{\text{old}} + \omega_p (p_{(i,j)}^{\text{new}} - p_{(i,j)}^{\text{old}}) \]  
(20)

\[ h_c^{\text{new}} = h_c^{\text{old}} + \omega_F (F_c - F_n) \]  
(21)

where the relaxation factors \( \omega_p \) and load relaxation factor \( \omega_F \) are taken to be 0.1 and 5.0 × 10\(^{-3}\), respectively. Symbol \( F_c \) denotes the load calculated according to the integral of the grinding fluid pressure over the grinding area.

Step 5: Based on the obtained pressure \( p \) and thickness \( h \) of the grinding fluid, the average speed \( U_a \) of the grinding fluid can be calculated according to Eq. (15). Meanwhile, the value of \( h_f \) is computed according to Eq. (13) by using the obtained average speed of the grinding fluid.

Step 6: Calculate the heat partition ratios \( R_{ws} \) of the workpiece-wheel, heat partition ratios \( R_{wch} \) of the
workpiece-chip, and heat distribution ratio $R_w$ of workpiece according to Eqs. (6) to (8), combined with the obtained $h_f$ in Step 5.

Step 7: Based on the Jaeger's moving heat source theory, use the triangular heat flux distribution to solve the temperature equation expressed with Eq. (1) for the grinding temperature based on the obtained heat distribution ratio and real length $l_c$ of contacting arc.

3 Model verification

The grinding experiments of alloy steel bearing track under different working conditions were performed on a grinding temperature measurement system to verify the proposed model. As shown in Fig. 3a, the overall system is mainly consisted of grinding system, signal processing system, and data collector. The grinding system is

Fig. 3  Schematic of grinding temperature measurement experiment: a measuring instrument and b grinding system
composed of the CNC grinding machine, grinding wheel, and 9310 alloy steel bearing track. The whole grinding experiments were performed on the KC33 CNC grinding machine shown in Fig. 3b, in which the aluminium oxide wheel with a 200mm radius and 25mm width (type 54A80 H8V604W) and Castrol 981 grinding fluid were used. Meanwhile, the density $\rho_f$, viscosity $\eta_f$, specific heat capacity $c_f$, and thermal conductivity $k_f$ of the grinding fluid are set as 890 kg/m$^3$, 0.011 Pa·s, 2096 J/(kg·K), and 0.15 W/(m·K), respectively.

When the grinding wheel became blunt in operation, the angle trimming method with $R$ was used for dressing the grinding wheel. It should be pointed out that the slip ring mounted on the grinding fixture was used to prevent the short circuit and wire winding among the measured wires under the wet grinding condition. Moreover, the double-pole Omega 5SRTC-GG-K-30-72 thermocouple was placed within the bearing track surface to measure the temperature signal of whole bearing track surface under wet grinding condition, which was insensitive by the environment and not required to be calibrated. When a single pass of the grinding wheel over the measuring junction, the temperature signals were obtained through the signal processing system.

In order to reduce the machine damage and the interference of machining vibration on temperature signal, the speed of bearing track and radial infeed rate were separately fixed at 20 r/min and 0.3 mm/min (i.e., the grinding depth was 15 μm). The applied grinding wheel speed was ranged from 27 to 35 m/s in the grinding experiment. The Young’s modulus and Poisson’s ratio for the grinding wheel are separately 49.6 MPa and 0.22, and the corresponding values for bearing track are 200 MPa and 0.29. During the grinding temperature measurement, the test in each working condition was repeated three times to obtain the average temperature.

Figure 4 shows the experimental and simulated results at different wheel speeds. The maximum temperature for the alloy steel material of 69.6 °C only is found due to the small grinding depth (i.e., 15 μm) and low grinding wheel speeds (i.e., 27–35 m/s) in this experiment. A similar low grinding temperature were also found in the literatures [33–36, 39]. The above temperature is lower than that of 503 °C for titanium alloy in literature [37]. This difference is mainly caused by the low thermal conductivity of the titanium alloy material. However, it should be pointed out that the temperature distribution in the grinding contact arc obtained from the experiment in a grinding cycle agrees well with the corresponding simulated result, in which the maximum grinding temperature is located in the central section of grinding arc zone. Such feather is consistent with the hypothesis of triangular heat source distribution. Moreover, there exist many temperature spikes in the experimental result owing to the interaction between the individual abrasive grains and bearing track.

The comparisons of the maximum grinding temperatures at different wheel speeds are further shown. It can be seen from Fig. 5 that the predicted temperatures considering the lubrication effect of the grinding fluid are in good agreement with the experimental temperatures. However, there exits a large error between the experimental result and simulation without lubrication effect of the grinding
4 Results and discussion

In the simulation, the 9310 alloy steel bearing track (i.e., workpiece), the white corundum grinding wheel, and the Castrol 981 grinding fluid are still used. In order to overcome the limitations of tested conditions, the speed of grinding wheel and radial infeed rate were separately improved to 90 m/s and 1.5 mm/min, and other main input parameters shown in Table 1 are used in the numerical calculation and they keep unchanged until otherwise specified. In the actual high-speed grinding, the impact of the air barrier layer on the penetration of grinding fluid is weakened through the increment in the jet velocity of grinding fluid [37] and install of the air baffle. Therefore, the air barrier layer under the condition of high speed grinding is not considered in the following simulation.

4.1 Wheel speed effect

Figure 6 shows variation of the maximum wet grinding temperature $T_{\text{max}}$ of workpiece at the varied grinding wheel speed $v_s$. As shown in Fig. 6, the values of $T_{\text{max}}$ with and without the lubrication effects of grinding fluid increase with increasing value of $v_s$. This is illustrated through the variation in the grinding temperature considering the lubrication effect. As seen from Fig. 7, when the value of $v_s$ varies from 50 to 110 m/s, the maximum simulated grinding temperature of the workpiece marked off with point A rises from 141.1 to 173.0 °C. Moreover, the location of the maximum grinding temperature appears on the surface of workpiece (i.e., $z = 0.0$ mm) and gradually moves away from the trailing edge (i.e., $l_c = 0.0$ mm) of the grinding arc zone. In this case, the grinding contact arc length $l_c$ of point A varies from 1.09 in Fig. 7a to 0.99 in Fig. 7d. This is explained through the effect of the grinding wheel speed $v_s$ on the workpiece heat flux $q_w$. As illustrated in Fig. 8a, the values of $q_w$ with and without the lubrication effects increase with increasing value of $v_s$, which makes the corresponding wet grinding temperatures increase. A similar phenomenon can be also found in literature [27] about the temperature for cylindrical wet grinding temperature.

Another important phenomenon in Fig. 6 is that at the same value of $v_s$, the value of $T_{\text{max}}$ with the lubrication effect is larger than that without the lubrication effect. Moreover, their relative difference becomes large when the value of $v_s$ increases, which means that reasonably choosing the large value of $v_s$ can enhance the lubrication effect of the grinding fluid. As seen from Fig. 8a, the value of $q_w$ with the lubrication effect is larger than that without the lubrication effect, and their difference gradually becomes large with increasing value of $v_s$. Correspondingly, when the lubrication effect is considered, the enhanced grinding temperature of the workpiece is obtained.

The above effect of the grinding heat flux $q_w$ on the grinding temperature is further explained. The value of $q_w$ is proportional to the total grinding heat flux $q$ and heat partition ratio $R_w$ of the workpiece, which can be observed from Eq. (5). As seen from the right longitudinal coordinate

---

Table 1: Input parameters

| Parameter                     | Value |
|-------------------------------|-------|
| Wheel speed $v_s$ (m/s)       | 90    |
| Workpiece speed $n_w$ (r/min) | 20    |
| Radial infeed rate of wheel $f_r$ (mm/min) | 1.5  |
| Diameter of wheel $D_s$ (mm)  | 200   |
| Young’s modulus of wheel $E_s$ (GPa) | 49.6 |
| Poisson’s ratio of wheel $\nu_s$ | 0.22 |
| Diameter of workpiece $D_w$ (mm) | 55   |
| Young’s modulus of workpiece $E_w$ (GPa) | 200  |
| Poisson’s ratio of workpiece $\nu_w$ | 0.29 |
| Width of wheel $B_s$ (mm)     | 40    |
| Width of workpiece $B_w$ (mm)  | 127   |
| Density of workpiece $\rho_w$ (kg/m$^3$) | 7850 |
| Specific heat capacity of workpiece $c_w$ (J/(kg·K)) | 472   |
| Thermal conductivity of workpiece $k_w$ (W/(m·K)) | 51.9  |
| Density of wheel $\rho_s$ (kg/m$^3$) | 1723  |
| Specific heat capacity of wheel $c_s$ (J/(kg·K)) | 765   |
| Thermal conductivity of wheel $k_s$ (W/(m·K)) | 35    |
| Density of grinding fluid $\rho_f$ (kg/m$^3$) | 890   |
| Viscosity of grinding fluid $\eta_f$ (Pa·s) | 0.011 |
| Specific heat capacity of grinding fluid $c_f$ (J/(kg·K)) | 2096  |
| Thermal conductivity of grinding fluid $k_f$ (W/(m·K)) | 0.15  |
in Fig. 8a, the values of $q_t$ for cases with and without lubrication effects both increase with increasing value of $v_s$. As shown in the left longitudinal coordinate in Fig. 8b, at the same the value of $v_s$, the convective heat transfer coefficients $h_f$ with the lubrication effect is smaller than that without the lubrication effect. Thus, the value of $R_w$ for the former is

![Diagram](image-url)

**Fig. 7** Simulated grinding temperature distributions for workpiece at varied grinding wheel speed: a $v_s = 50$ m/s, b $v_s = 70$ m/s, c $v_s = 90$ m/s, and d $v_s = 110$ m/s

![Diagram](image-url)

**Fig. 8** Grinding process parameters at varied grinding wheel speed: a heat flux into the workpiece and total grinding heat flux and b convective heat transfer coefficient and heat partition ratio of workpiece
larger than that for the latter according to Eq. (6). Combining the above analysis, the value of \( q_w \) with the lubrication effect is larger compared with that without the lubrication effect, and further the grinding temperature for the former is higher than that for the latter.

The difference between the values of \( h_f \) with and without the lubrication effects is mainly caused by the grinding fluid speed. As stated previously, the grinding fluid speed with the lubrication effect is defined as the grinding fluid average speed \( U_a \), and the grinding fluid speed without the lubrication effect is evaluated through the grinding wheel speed \( v_s \). As seen from Fig. 9, with the increment in the value of \( v_s \), the value of \( U_a \) increases due to the enhanced hydrodynamic action. When the value of \( v_s \) is fixed, the value of \( U_a \) is smaller than that of \( v_s \), and their difference becomes large when the value of \( v_s \) increases, which further indicates that the enhanced lubrication effect of the grinding fluid is obtained when the value of \( v_s \) is improved. Thus, according to Eqs. (12) and (13), the value of \( h_f \) with the lubrication is smaller than that without the lubrication. Since a larger value of \( h_f \) means that more grinding heat is carried by the grinding fluid, high grinding temperature yields for the lubrication case.

### 4.2 Workpiece speed effect

Figure 10 shows variations of the maximum wet grinding temperature \( T_{max} \) of workpiece under different workpiece speeds \( n_w \). As shown in Fig. 10a, with the increase in the value of \( n_w \), the values of \( T_{max} \) with and without the lubrication effects of grinding fluid decrease. Such a phenomenon differs from the numerical and experimental results in literatures [38–40], in which the value of \( T_{max} \) without the lubrication effect increases with increasing value of \( n_w \). The above phenomena in the values of \( T_{max} \) with and without lubrication effects can be explained through the grinding heat flux entering into the workpiece and grinding arc length. As illustrated in Fig. 10b, all the four types of grinding heat fluxes \( q_w \) increase with increasing value of \( n_w \). Increasing the value of \( n_w \), however, brings out the reduction of the grinding contact arc length \( l_c \) according to Eq. (2). Therefore, the reduced grinding temperature of workpiece occurs. A similar phenomenon is also demonstrated through the grinding temperature considering the lubrication effect. As shown in Fig. 11, when the lubrication effect is considered, the grinding temperatures \( T \) with the lubrication effect along the contact arc length direction and depth direction of the workpiece \( z \) drop off with increasing value of \( n_w \).

It should also be pointed out that the value of \( T_{max} \) with the lubrication effect shown in Fig. 10a is larger than that without the lubrication effect, and their relative difference is reduced with the increment in the value of \( n_w \), implying that the lubrication effect of grinding fluid is more obvious.
at the low value of $n_w$. As illustrated in Fig. 10b, the value of $q_w$ with the lubrication effect is larger than that without the lubrication effect, and this trend becomes weak at the large value of $n_w$. In addition, when the value of $n_w$ increases, the weakened grinding contact arc lengths $l_c$ with and without the lubrication effects are obtained, which also can be observed from Eq. (2). Therefore, a larger grinding temperature of the workpiece occurs when the lubrication effect is considered.

The reason for the difference of the grinding heat fluxes $q_w$ with and without the lubrication effects on the grinding temperature is further revealed. When the lubrication effect is considered, at the same the value of $n_w$, the grinding fluid speed is perceived to be the same as the average speed $U_a$, which is smaller than the grinding fluid speed without the lubrication effect (i.e., grinding wheel speed $v_s$), as shown in Fig. 12a. In addition, the difference between the values of $U_a$ and $v_s$ decreases with increasing value of $n_w$, which means that the large value of $n_w$ can weaken the lubrication effect of the grinding fluid. Together with Eqs. (12) and (13), the convective heat transfer coefficient $h_f$ with the lubrication effect shown in Fig. 12b is smaller than that without the lubrication effect. The larger value of $h_f$ implies the lower grinding heat flux $q_w$ entering into the workpiece. Therefore, a larger grinding temperature yields once the lubrication effect of the grinding fluid is considered.

Another phenomenon in Fig. 12a is that the value of $U_a$ with the lubrication effect increases with increasing value of $n_w$, which is caused by the change of the lubrication
performance of grinding fluid. As illustrated in Fig. 13a, the hydrodynamic pressure \( p \) supporting the grinding wheel decreases with increasing value of \( n_w \) due to the weakened hydrodynamic effect. Meanwhile, the normal grinding force \( F_n \) shown in Fig. 13b decreases with increasing value of \( n_w \), which weakens the squeezing effect by the grinding wheel and further makes the hydrodynamic pressure become small. As a result, an enhanced value of \( U_a \) yields when the value of \( n_w \) increases.

### 4.3 Radial infeed rate effect

Effects of radial infeed rates \( f_r \) on the maximum wet grinding temperature of workpiece \( T_{max} \) are also investigated. As illustrated in Fig. 14, the increase in the value of \( f_r \) results in increments in the values of \( T_{max} \) with and without the lubrication effects of grinding fluid. Increasing the value of \( f_r \) leads to an improvement in the grinding heat fluxes \( q_w \), regardless of the lubrication effect is or not considered, as shown in Fig. 15a. Besides, at the large value of \( f_r \), the enhanced grinding arc length \( l_c \) is easily obtained, as illustrated in the right longitudinal coordinate in Fig. 15b. Combining the above two factors, the enlarged values of \( T_{max} \) with and without the lubrication effects yield through the moving heat source theory in Eq. (1).

In addition, the value of \( T_{max} \) with the lubrication effect in Fig. 14 is larger than that without the lubrication effect, and their relative difference becomes large with increasing value of \( f_r \), indicating that the large value of \( f_r \) can enhance the lubrication effect of the grinding fluid. The reason is that the grinding heat flux \( q_w \) with the lubrication effect is larger than that without the lubrication effect at the fixed \( f_r \). And this phenomenon becomes obvious at the larger value of \( f_r \), as shown in Fig. 15a. Besides, it can also be observed from Fig. 15b that increasing the value of \( f_r \) also brings out the enhanced grinding contact arc length \( l_c \), so a larger grinding temperature of the workpiece occurs if the lubrication effect is considered.

The effect of \( q_w \) on the grinding temperature is further disclosed. As illustrated in Fig. 16a, the convective heat transfer coefficients \( h_f \) with and without the lubrication effects decrease with increasing value of \( f_r \). Moreover, the former value is lower than that of the latter. As seen from Fig. 16b, however, the heat partition ratio \( R_w \) of the workpiece with the lubrication effect is larger than that without the lubrication effect. This implies that the strong grinding heat flux \( q_w \) entering into the workpiece occurs in the case of lubrication, therefore high grinding temperature appearing.
5 Conclusions

The improved cylindrical wet grinding temperature (ICWGT) model considering the lubrication effect of the grinding fluid is established, in which the hydrodynamic pressure of grinding fluid is considered to evaluate the convective heat transfer coefficient (CTHC) and to calculate the elastic deformation. In doing so, the Newton–Raphson method is employed to solve the hydrodynamic pressure, and CTHC of the grinding fluid is further obtained based on heat transfer theory. Meanwhile, the moving heat source method is adopted to calculate the grinding temperature rise, and the fast Fourier transform method is used to accelerate the deformation calculation of the workpiece.

Further, the influence of grinding process parameters on the cylindrical wet grinding temperature was revealed. The associated conclusions are drawn as follows:

1. The ICWGT considering the lubrication effect of the grinding fluid is established. The alloy steel grinding experiment shows that the proposed model, relative to the method without considering the lubrication, can more accurately predict the workpiece grinding temperature.
2. The simulation result shows that the grinding temperature considering the lubrication effect of grinding fluid is larger than that without the consideration this lubrication effect and closer to its corresponding experimental result.
3. The grinding temperature considering the lubrication effect of grinding fluid decreases with the increment of the workpiece speed due to the decrease of the contact area length and hydrodynamic pressure of the grinding fluid.

4. The grinding temperature considering the lubrication effect of grinding fluid increases with the increment of the radial infeed rate due to the reduction of the grinding fluid CHTC and increment of the heat flux into the workpiece.

5. An enhanced lubrication effect of the grinding fluid is obtained by increasing the grinding wheel speed and radial infeed rate and decreasing the workpiece speed.

6. The proposed method can also be suitable for accurately evaluating the grinding temperature of the cylindrical wet grinding for other types of steel.

**Author contribution** Yong Zheng: investigation, methodology, software, validation, data curation, writing—review and editing. Changqing Wang: investigation, software, data curation, writing—original draft. Yifei Zhang: investigation, formal analysis, visualization. Fanning Meng: conceptualization, funding acquisition, project administration, supervision, writing—review and editing.

**Funding** This study was supported by the National Natural Science Foundation of China (Nos. 52175160 and 51975381), and the Research and Innovation Projects of Graduate Students in Chongqing City (No. CYS19003).

**Data availability** All data supporting the findings of this study are included in this article.

**Code availability** The method codes involved in this paper are not applicable.

**Declarations**

**Ethics approval** The authors declare that this manuscript was not submitted to more than one journal for simultaneous consideration. The submitted work is original and has not been published elsewhere in any form or language.

**Consent to participate and publish** The authors declare that they consent to participate in and publish this paper.

**Conflict of interest** The authors declare no competing interests.

**References**

1. Rowe WB (2009) Principles of modern grinding technology. William Andrew Publishing, USA

2. Lin B, Zhou K, Guo J, Liu QY, Wang WJ (2018) Influence of grinding parameters on surface temperature and burn behaviors of grinding rail. Tribol Int 122:151–162

3. Jaeger JC (1942) Moving sources of heat and the temperature at sliding contacts. J Proc Roy Soc New South Wales 76:203–204

4. Desruisseaux NR, Zerkle RD (1970) Temperature in semi-infinite and cylindrical bodies subjected to moving heat sources and surface cooling. J Heat Trans 92:456–464

5. Kim NK, Guo C, Malkin C (1997) Heat flux distribution and energy partition in creep-feed grinding. CIRP Ann Manuf Technol 46:227–232

6. Jin T, Stephenson DJ (2006) Heat flux distributions and convective heat transfer in deep grinding. Int J Mach Tools Manuf 46:1862–1868

7. Li BZ, Zhu DH, Pang JZ, Yang JG (2011) Quadratic curve heat flux distribution model in the grinding zone. Int J Adv Manuf Technol 54:931–940

8. Lavas AS (1998) A simple model for convective cooling during the grinding process. J Eng Ind 110:1–6

9. Miao Q, Li HN, Ding WF (2020) On the temperature field in the creep-feeding grinding of turbine blade root: Simulation and experiments. Int J Heat Mass Tran 147:118957–1119013

10. Rowe WB, Morgan MN, Allanson DR (1991) An advance in the modelling of thermal effects in the grinding process. CIRP Ann Manuf Technol 40:339–342

11. Zhang L, Rowe WB, Morgan MN (2013) An improved fluid convection solution in conventional grinding. Proc Inst Mech Eng B J Eng Manuf 227:832–838

12. Lin B, Morgan MN, Chen XW, Wang YK (2009) Study on the convection heat transfer coefficient of coolant and the maximum temperature in the grinding process. Int J Adv Manuf Technol 42:1175–1186

13. Chang CC (1997) An application of lubrication theory to predict useful flow-rate of coolants on grinding porous media. Tribol Int 30:575–581

14. Jin T, Stephenson DJ, Rowe WB (2003) Estimation of the convection heat transfer coefficient of coolant within the grinding zone. Proc Inst Mech Eng B J Eng Manuf 217:397–407

15. Li CH, Mao W, Hou YL, Ding YC (2011) Investigation of hydrodynamic pressure in high-speed precision grinding. Procedia Eng 15:2809–2813

16. Hryniewicz P, Szeri AZ, Jahanmir S (2001) Application of lubrication theory to fluid flow in grinding: Part I—flow between smooth surfaces. J Tribol 123:94–100

17. Hryniewicz P, Szeri AZ, Jahanmir S (2001) Application of lubrication theory to fluid flow in grinding: Part II—influence of wheel and workpiece roughness. J Tribol 123:101–107

18. Meng FM, Cui XF, Pu C (2019) Effect of three-dimensional surface crack on the elastohydrodynamic lubrication performance of ellipsoidal contact. Proc Inst Mech Eng J Tribol 233:975–991

19. Liu SB, Wang QJ, Liu G (2000) A versatile method of discrete convolution and FFT (DC-FFT) for contact analyses. Wear 243:101–111

20. Meng FM (2013) On influence of cavitation in lubricant upon tribological performances of textured surfaces. Opt Laser Technol 48:422–431

21. Rowe WB, Qi HS, Morgan MN, Zhang HW (1993) The effect of deformation in the contact area in grinding. CIRP Ann Manuf Technol 42:409–412

22. Zhu XH (1988) Grinding principle. China Machine Press, Beijing

23. Li GF, Wang LS (2001) Simulation and optimum for plunge grinding. Key Eng Mater 202–203:219–226

24. Black SCE (1996) The effect of abrasive properties on the surface integrity of ground ferrous materials. Doctoral thesis, Liverpool John Moores University, Liverpool

25. Rowe WB (2017) Temperatures in grinding—a review. J Manuf Sci E 139:121001:1–121006

26. Jin T, Stephenson DJ (2006) Analysis of grinding chip temperature and energy partitioning in high-efficiency deep grinding. Inst Mech Eng B J Eng Manuf 220:615–625
27. Thanedar A, Dongre GG, Joshi SS (2019) Analytical modelling of temperature in cylindrical grinding to predict grinding burns. Int J Precis Eng Man 20:13–25
28. Rowe WB, Black S, Mills B, Qi MNMS (1997) Grding temperatures and energy partitioning. P Roy Soc A 453:1083–1104
29. Bai XY, Dong QB, Zheng H, Zhou K (2021) Modelling of non-Newtonian starved thermal-elastohydrodynamic lubrication of heterogeneous materials in impact motion. Acta Mech Solida Sin 34(6):954–976
30. He D, Dong QB, Zhao G (2021) Modeling mixed lubrication in point and line contact by non-normalized discretization. Int J Appl Mech 13(07):2150080-1-23
31. Bai XY, Dong QB, Zheng H, Zhou K (2021) A finite element model for non-Newtonian starved thermal-elastohydrodynamic lubrication of 3D line contact. Int J Appl Mech 13(09):2150107-1-32
32. Dong QB, Zhou K (2015) Modeling heterogeneous materials with multiple inclusions under mixed lubrication contact. Int J Mech Sci 103:89–96
33. Sakakura M, Ohnishi T, Shinoda T (2012) Temperature distribution in a workpiece during cylindrical plunge grinding. Prod Eng Res Devel 06:149–155
34. Nie ZG, Wang G, Wang LP, Rong YM (2019) A coupled thermomechanical modeling method for predicting grinding residual stress based on randomly distributed abrasive grains. J Manuf Sci E 141:081005-1–81012
35. Mihić SD, Cioc S, Marinescu ID, Weismiller MC (2013) Detailed study of fluid flow and heat transfer in the abrasive grinding contact using computational fluid dynamics methods. J Manuf Sci E 141:041002-1–41012
36. Pang JZ, Wu CJ, Shen YM, Liu SQ, Wang QG, Li PZ (2019) Heat flux distribution and temperature prediction model for dry and wet cylindrical plunge grinding. Proc Inst Mech Eng B J Eng Manuf 233:2047–2060
37. Ding ZS, Jiang XH, Guo MX, Liang SY (2018) Investigation of the grinding temperature and energy partition during cylindrical grinding. Int J Adv Manuf Technol 97:1767–1778
38. Yin GX, Marinescu ID (2017) A Heat transfer model of grinding process based on energy partition analysis and grinding fluid Cooling application. J Manuf Sci E 139:121015–121111
39. Zhang L, Rowe WB (2020) Study of convective heat transfer in grinding applied to tool carbide. J Manuf Sci E 142:021001–021009
40. Yi J, Jin T, Zhou W, Deng ZH (2020) Theoretical and experimental analysis of temperature distribution during full tooth groove form grinding. J Manuf Process 58:101–115

Publisher's Note Springer Nature remains neutral with regard to jurisdictional claims in published maps and institutional affiliations.