Topical Review

Friction behaviors in the metal cutting process: state of the art and future perspectives

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Abstract

Material removal in the cutting process is regarded as a friction system with multiple input and output variables. The complexity of the cutting friction system is caused by the extreme conditions existing on the tool–chip and tool–workpiece interfaces. The critical issue is significant to use knowledge of cutting friction behaviors to guide researchers and industrial manufacturing engineers in designing rational cutting processes to reduce tool wear and improve surface quality. This review focuses on the state of the art of research on friction behaviors in cutting procedures as well as future perspectives. First, the cutting friction phenomena under extreme conditions, such as high temperature, large strain/strain rates, sticking–sliding contact states, and diverse cutting conditions are analyzed. Second, the theoretical models of cutting friction behaviors and the application of simulation technology are discussed. Third, the factors that affect friction behaviors are analyzed, including material matching, cutting parameters, lubrication/cooling conditions, micro/nano surface textures, and tool coatings. Then, the consequences of the cutting friction phenomena, including tool wear patterns, tool life, chip formation, and the machined surface are analyzed. Finally, the research limitations and future work for cutting friction behaviors are discussed. This review contributes to the understanding of cutting friction behaviors and the development of high-quality cutting technology.

Keywords: cutting process, friction behaviors, material removal process, contact condition

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1. Introduction

Cutting processes play a central role in industrial fields, where more than 15% of all mechanical components are manufactured through cutting operations [1, 2]. As shown in figure 1, achieving high-efficiency machining on difficult-to-cut materials is directly related to the overall level of critical industrial sectors, such as aviation, aerospace, and automobiles [3, 4]. The critical distinction between cutting and other metal-forming processes is the fracture process of material separation into the workpiece and chip, where the contact interface is accompanied by intense friction resulting from shear, extrusion, and deformation [5]. During metal cutting, more than 20% of the total energy is used to overcome the friction at the tool–chip and tool–workpiece contact interfaces, and the cutting friction behaviors have essential influences on the cutting process [6, 7]. With the rapid development of manufacturing technology in the direction of ‘high precision, high efficiency, intelligence, compound, and green,’ the basic theory and application technology of cutting friction have advanced significantly.

From the perspective of tribology, friction behaviors in metal cutting are unique and challenging. As shown in figure 2, friction in cutting processes is distinct from other manufacturing processes. The material removal through the cutting process is regarded as the friction system with multiple input and output variables. In this system, the complexity stems from the extreme conditions, such as high temperature (>1000 °C) [8, 9], large strain (1–10) [10, 11], high strain rate (up to 10^6 s^{-1}) [12], the sticking–sliding contact mode, and diverse cutting conditions at the tool–chip and tool–workpiece interfaces [13]. As a result, friction behaviors depend highly on the cutting process [14]. As shown in figure 3, the cutting process involves complex thermo-mechanical loads caused by shear deformation and friction [15]. Furthermore, friction behaviors vary locally because the contact loads are concentrated in a limited region. The friction stress exceeds the shear flow stress of the workpiece, especially during the transition from a sliding state to a sticking state [16, 17]. In addition, friction behaviors are dynamic due to the interfacial chemical reaction, newly generated surface, and progressive tool wear [18]. Therefore, identifying the thermo-mechanical loads and contact boundary conditions is extremely important for controlling the cutting friction behaviors.

In the cutting process analysis, developing friction behavior models has been challenging. The descriptions of contact conditions based on Coulomb’s law friction coefficient are unrealistic. The friction coefficient of the cutting contact interface should not be taken as a constant state but rather as a cutting friction system with variables interacting with the material characteristics and thermo-mechanical loads [19]. Based on the complex friction interaction’s limited understanding, the cutting process is difficult to distinguish from the piecewise friction coefficient of the contact region, and the overall friction data are only obtained through limited experimental methods. Since the cutting process involves a variety of complex physical phenomena, it is difficult to establish a mathematical description of the friction characteristics using quantitative models [20]. Moreover, friction types can change under diverse cutting conditions, which are difficult to distinguish from the interface boundary conditions. To reduce cost, appropriate friction models, such as numerical and empirical models, are needed to optimize cutting processes [21]. Therefore, establishing a cutting friction model that considers multiple factors is necessary, especially for embedding into simulation applications.

Friction behaviors during the cutting process are affected by a variety of factors, including the material matching for the workpiece and cutting tool, the cooling medium and lubrication mode, the optimization of cutting parameters, the modification of the tool contact surface through micro/nano texture surface technology and tool coating technology [22–25]. These factors, directly and indirectly, affect the friction coefficient by changing the cutting friction behaviors and the study of a comprehensive relationship is urgently necessary. The critical problem is determining how to use theoretical knowledge to control friction behaviors in the high-efficiency cutting process.

Cutting friction behaviors inevitably occur, affecting tool wear, life, chip formation, and surface integrity. The harsh cutting conditions generate local thermo-mechanical gradients at the contact interface, resulting in changes to the thermophysical properties of tool materials [26]. As a result, tool wear occurs because of physical processes, chemical reactions, and thermo-mechanical phenomena. Moreover, tool life reduction leads to an increase in manufacturing costs. As shown in figure 4, tool wear changes the interface contact mode and increases the additional thermo-mechanical loads. The cutting friction at the tool–chip interface forms the secondary shear deformation zone, which directly impacts chip formation. The thermo-mechanical loads of friction intensification at the tool–workpiece interface have become a pivotal factor affecting surface integrity, resulting in gradient changes in the surface topography, microstructure, and mechanical properties [27, 28].
Although the cutting process has been studied extensively, a comprehensive understanding of friction behaviors is needed. Therefore, in this article, the state-of-the-art and future perspectives on friction behaviors in cutting processes are reviewed and analyzed. Figure 5 illustrates the framework of this review as follows. Section 2 analyzes the phenomena of cutting friction under extreme conditions, such as elevated temperature, large strain, high strain rate, the sticking–sliding contact mode, and diverse cutting conditions. In sections 3 and 4, the theoretical models of cutting friction behaviors and the application of simulation technology are reviewed. The factors that affect friction behaviors, including material matching, cutting parameters, lubrication/cooling environment, micro/nano surface texture, and tool coatings,
are analyzed in section 5. Section 6 discusses the consequences of friction behaviors, including tool wear, tool life, chip formation, and the machined surface. The conclusions are given in section 7, and the research limitations and directions for future work are described in section 8. This review contributes to the understanding of cutting friction behaviors and the development of high-quality cutting technology.

2. Friction behaviors under extreme contact conditions

2.1. Elevated cutting temperature

Friction behaviors are affected by heat generation during the cutting process. More than 90% of the mechanical and deformation energy is converted into heat flux, significantly increasing the cutting temperature. The process by which the cutting temperature affects the friction behaviors in the cutting of metals is complex. The cutting temperature depends on the generation and dissipation of cutting heat. As a result, the friction heat was generated at the contact interface, causing severe high-temperature friction behaviors. The regions involved the secondary shear region at the tool–chip interface and the third deformation region at the tool–workpiece interface [41]. From the perspective of internal factors, increased frictional heat led to increased cutting temperature. Among the external factors, the lubricants, coatings, and micro texturing could reduce friction and improve heat dissipation, reducing the adverse effects of the high cutting temperature on tools and machined surfaces. Moreover, increased cutting temperature caused rapid tool wear, worsening the friction conditions.

Understanding the friction behaviors requires precise measurement of the cutting temperature at the friction interface. Selecting reliable temperature measurement technology is challenging owing to the limitation of the cutting contact interface. Several researchers used infrared thermal imaging and thin-film thermocouples to create a customized gadget for high-resolution temperature measurement [8, 42]. In addition, simulation technology has been used to study the interface friction temperature. Figure 6 displays the cutting temperature distribution at the friction interface using experimental and simulation methods during the cutting process. These findings indicated that the maximum value of the cutting temperature (as much as 1000 °C) appeared at the tool–chip contact...
interface. The location of the high-temperature interface was sufficient to prove the importance of the cutting friction process. In addition, the flank wear length effect on the cutting temperature distribution was studied, indicating that the additional cutting friction enhanced the temperature at the tool–workpiece interface with the progress of tool wear [35].

### 2.2. Large strain, high strain rate, and extreme deformation

The material removal process generally involves severe plastic deformation, accompanied by the shear yield behavior under a large strain and high strain rate. Strain and strain rate research has mainly focused on numerical calculation and simulation model analysis based on the material constitutive law. Several studies were also supplemented by advanced observation experiments, such as in-situ particle imaging velocimetry and photo-elastic experiments [10, 43]. Figure 7 presents the strain and strain rate distribution in the cutting friction interface using experimental and simulation methods. The strain/strain rate distribution in the cutting region is complex, especially with a strain up to 10 and a strain rate close to $5 \times 10^4$ s$^{-1}$. Moreover, large local plastic strain and high strain rate caused by increased friction contact stress were quantitatively characterized in terms of the deformation behaviors of the superficial microstructure by image-based processing [44].

### 2.3. Sticking–sliding contact state

Friction behavior analysis involves a critical understanding of the cutting contact mode. Because plastic volumetric deformation altered the development of the contact condition, the cutting friction could not be regarded as a pure surface effect [46]. Moreover, the interface friction in the cutting process was not a simple sliding friction process but involved the complex sticking contact state [47]. The balance between sticking and sliding forms could change with the dynamic cutting friction conditions. As illustrated in figure 8, the contact condition of the tool–chip interface is altered with the increase in distance from the cutting edge, which is divided into three zones: sticking, adhesion, and sliding [48]. The effects of pure sliding friction were challenging to identify while simultaneously considering the ‘viscous’ effect in the various contact states. In addition, the existence of two different friction contact states on the tool wear surface was confirmed, and the sticking and sliding regions were separated by a clear boundary [49]. Several researchers also measured the velocity field of the chip flow at the cutting contact region to determine the nature of the stagnant material at the front of the cutting edge [29]. Investigating such a phenomenon further enhanced the understanding of the sticking–sliding contact condition [50].

The cutting process involved three friction locations: the tool–chip, cutting–edge arc, and tool–workpiece interfaces. Because the friction region was extremely small, the contact states at different locations on the cutting tool were diverse with a mixture of sticking and sliding states. The sticking, sliding, and stagnant contact regions around the cutting tool edge are shown in figure 9 [51]. The cutting edge is not sharp, which
would plough the local machined surface rather than remove any material. As a result, friction behaviors are generated by the arc extrusion of the cutting edge and material deformation. In addition, the stress distribution of the contact interface further proved that distinct types of friction behaviors manifested themselves on the tool rake face, edge position, and tool flank face [52].

2.4. Diverse cutting conditions

Advanced manufacturing technology is developing in the direction of ultra-high speed, high flexibility, ultra-precision, and green processing. The diverse cutting processes mainly include ultra-high speed machining, heavy-duty cutting, laser-assisted machining, precision, and ultra-precision cutting [53, 54]. As shown in figure 10, the cutting mechanism of ultra-high speed machining also undergoes substantial alterations concerning traditional cutting when the cutting speed exceeds 1000 m min\(^{-1}\). Ultra-high speed machining involves severe thermo-mechanical loads and material deformation, further complicating the cutting friction behaviors [55]. Under extreme heavy-duty cutting conditions, severe tool wear is induced by impact damage and interface friction [26]. Moreover, the friction issues in precision and ultra-precision manufacturing are especially pronounced because of the size effect [40, 56]. As the depth of cut approaches the tool edge radius, the cutting-dominant pattern gradually transforms into cutting shear, ploughing, and adhesive friction [57, 58]. The material plastic flow at the tool/workpiece interface elucidates the shear-dominated chip formation in conventional cutting and extrusion-dominated piled-up material in micro/nano cutting. The ploughing contact area near the cutting edge leads to retardation of material deformation, which is also accompanied by severe friction behaviors [59]. The tool wear mechanism is also gradually transformed from the rake face of tool–chip interface friction to the flank face of tool–workpiece.
interface friction. In addition, atomic and close-to-atomic scale cutting is characterized by the occurrence of shear stress-driven dislocation motion and elastic deformation of the machined surface [60, 61]. Although research on the wear mechanism during atomic sliding remains insufficient, its frictional behavior correlates with the actual atomic contact area during cutting [62]. Simultaneously, the cutting friction behaviors are also affected by a wide range of possible external conditions. When the tool coatings, micro/nano textures, and lubrication/cooling are introduced at the contact interface, the friction mechanics inevitably increase in complexity.

3. Theoretical models of friction behavior

Theoretical models are necessary to analyze the cutting friction behaviors and gain a better knowledge of the cutting process because of many potential influencing factors. Various friction models have been proposed by researchers to analyze friction behaviors based on an in-depth understanding of the cutting contact state. Although these models are useful for forecasting cutting friction, none of them can be considered entirely adequate. Table 1 summarizes and discusses the critical research on friction models using tribometers and cutting experiments.

Several studies used the constant friction coefficient based on the Coulomb friction model. As shown in figure 11, the Merchant model and its improvement were used to estimate the friction coefficient [63]. This method aimed to determine the average friction coefficient of the tool rake face following the cutting force components. A modified method of calculating the friction coefficient was also proposed considering the ploughing force [64]. However, these methods only determined the overall friction coefficient, which cannot reflect the diversity of the friction conditions among the tool, chip, and workpiece. Furthermore, the improved model proposed replacing the cutting force components with the corresponding contact area and stress distribution. The final model used the stress ratio to calculate the average friction coefficient [65]. The dynamic friction coefficient model was further developed according to the stress components at the ploughing region [34]. Several researchers relied on only the geometric model during the cutting process to evaluate the friction coefficient. The analytical formula for the friction coefficient was derived considering the actual tool angle, edge radius, and tool wear state and resulted without the necessity for stress or cutting force measurements [66]. The maximum friction coefficient was also evaluated by considering the tool rake angle [67]. Moreover, the wave contact model, wave removal model, and chip formation model were also established to calculate the relevant friction coefficient according to the slip line theory [68].

The cutting friction conditions were distinct from simple sliding friction, which was not explained by the classic Coulomb model because of the high friction stress. A model was established considering Coulomb friction in the sliding region and constant shear friction in the sticking region along the entire tool–chip interface [69]. A modified friction model was further proposed, replacing the constant friction coefficient with the influential plastic strain variables. In several studies, the apparent friction coefficient composed of an adhesive and a mechanical friction coefficient was also considered, which was calculated with the limited shear stress and shear angle [70]. Moreover, the friction coefficient at the tool–chip
### Table 1. Friction models by analysis of tribometers and cutting experiments.

| References          | Experimental conditions                      | Friction models                                                                 | Remarks                                                                                           |
|---------------------|---------------------------------------------|---------------------------------------------------------------------------------|---------------------------------------------------------------------------------------------------|
| Merchant [63]       | Orthogonal turning                          | $\mu = \frac{F_s}{F_N} = \frac{F_s \sin \gamma + F_t \cos \gamma}{F_s \cos \gamma + F_t \sin \gamma}$ | Only the overall friction coefficient was calculated by measuring cutting force components, but local friction behavior could not be reflected. |
| Abrecht [64]        | Orthogonal turning 45 steel with round tool edge radius under various depths of cut | $\mu = \frac{(F_y - F_r) + (F_x - F_r) \tan \gamma}{(F_y - F_r) + (F_x - F_r) \tan \gamma}$ | The consideration of ploughing force was added in the modified model.                               |
| Mackaw [65]         | Separation tool method in tuning steels     | $\mu = \frac{\tau_{BA}}{\tau_{AE}} = \frac{\tau}{\tau_{AE}}$                   | Replaced the cutting force with the actual contact area and stress distribution.                    |
| Chen et al [34]     | Ultrasonic vibration-assisted turn Ti-6Al-4V with tungsten carbide tools | $\mu = A_0/P_a \left[ \tau_{11} + \tau_{12} \cdot f(\tau_{11} H_1/\tau_{12} H_2) \right]$ | Dynamic friction coefficient model considering stress distribution of ploughing region.            |
| Du et al [66]       | Orthogonal turning Ti-6Al-4V with the uncoated carbide tool | $\mu = \tan \left( \frac{1}{2} + \gamma + \alpha \cdot \arccos \left( 1 - \frac{1}{2} \sin \alpha \right) \sin \alpha \right)$ | Considering the actual tool angle, edge radius, and tool wear state without needing stress measurements. |
| Grzesik [67]        | Turning steels with tungsten carbide tools  | $\mu_{\max} = \frac{1}{2} (1.3 \pm \gamma_0)$                                  | Maximum friction coefficient model considering the tool rake angle.                                 |
| Kopalinsky and Oxley [68] | Orthogonal turning 5083-H32 aluminum magnesium alloy | $\mu = \frac{1 - 2 \sin \beta + (1 - f^2)^{1/2}}{1 - 2 \sin \beta + (1 - f^2)^{1/2}} \sin \alpha + f \cos \alpha$ | Explaining the mechanics of sliding friction according to slip line theory.                        |
| Filice et al [69]   | Orthogonal turning AISI 1045 steel with an uncoated carbide tool | $\tau = mk$                                                                   | Considering constant shear friction in the sticking region and Coulomb friction in the sliding region. |
| Grzesik [70]        | Turning carbon steel and stainless steel with uncoated carbides | $\mu = \mu_a + \mu_m = \frac{2}{\pi} + \arctan \left( \frac{2}{\pi} - \phi + \gamma \right)$ | Considering the apparent friction coefficient composed of adhesive and mechanical friction coefficients. |
| Tabor [71]          | Orthogonal turning                          | $\mu \propto \frac{1}{r}$                                                      | Considering the relationship between the friction coefficient and normal stress.                    |
| Ozlu et al [72]     | Turning AISI 1050 and Ti-6Al-4V with CBN tools | $\mu_s = \tan \left( \lambda_s \right) = \frac{2}{\pi} \left[ 1 + \phi \left( 1 - \left( \frac{1}{\pi} \right)^{1/1} \right) \right]$ | Apparent friction considering the sticking phenomenon on the rake face.                             |
| Zorev [73]          | Orthogonal turning                          | $\tau = \left\{ \begin{array}{ll} \mu \sigma_a & \tau < \tau_{\max} \\ \tau_{\max} & \tau \geq \tau_{\max} \end{array} \right.$ | Dual-contact theory is based on normal and shear stress variations defined as sticking and sliding regions. |
| Calamaz et al [74]  | Orthogonal turning Ti-6Al-4V with carbide tools | $\tau = \left\{ \begin{array}{ll} \mu \sigma_a & \mu \sigma_a = m \sigma_0 / \sqrt{3} \\ \mu \sigma_a > m \sigma_0 / \sqrt{3} & \mu \sigma_a \geq m \sigma_0 / \sqrt{3} \end{array} \right.$ | Considering the Coulomb limited Tresca law at the tool–chip interface.                             |
| Shirakashi and Usui [75] | Separation tool method in tuning brass     | $\tau = k \left( 1 - e^{-m \sigma / k} \right)$                              | Considering the exponential relationship among shear stress, normal stress and friction coefficient. |
| Childs [76]         | Separation tool method in tuning steels     | $\tau_f = \frac{\sigma}{\gamma} \left( 1 - e^{-m \sigma \sqrt{3} / \gamma} \right)$ | Considering limited friction stress and equivalent effective flow stress.                          |
| Bahi et al [77]     | Turning 42CrMo4 steel under various temperatures | $\mu_s = \bar{\mu} / \left\{ \left( 1 - a / L_a \right)^{\zeta} \left[ 1 + \zeta \right] \frac{\rho_{\sigma}^{\zeta}}{\zeta} + \left( 1 - a / L_a \right) \right\}$ | Hybrid analytical approach identifying the sticking zone and associated friction parameters.        |

(Continued.)
flow stress according to the plastic flow criterion, the friction tool–chip interface. Because the shear stress did not exceed the contact based on normal and shear stress variations along the established a dual-contact zone theory defining sticking and sliding interface was also established based on the interfacial sticking theory [71, 72]. As shown in figure 12, Zorev [73] established a dual-contact zone theory defining sticking and sliding contact based on normal and shear stress variations along the tool–chip interface. Because the shear stress did not exceed the flow stress according to the plastic flow criterion, the friction stress in the sticking region was a function of the equivalent flow stress which decreased linearly to zero based on the Coulomb law in the sliding region. The friction coefficient of the tool–chip interface was also calculated by the Coulomb limited Tresca law [74]. Another research established an exponential relationship between the friction coefficient, shear

| References | Experimental conditions | Friction models | Remarks |
|------------|-------------------------|----------------|---------|
| Zhang et al [78] | Orthogonal turning AISI 1050 steel with TiAlN coating tool | $\mu(x) = \begin{cases} \mu_{a,c} \sigma_{d,2} & (0 \leq x \leq l_p) \\ \mu_{a,2} \sigma_{d,2} & (l_p \leq x \leq L) \end{cases}$ | Contact normal stress and relative slipping rate are based on the sticking friction theory. |
| Zhou [79] | Orthogonal turning 42CrMo4 steel with uncoated carbide tools | $\mu = \frac{C}{\mu_{c} + \mu_{s} / (1 - \frac{l_p}{L})^{p_{c}+1}}$ | Considering the thickness of the material transport layer and the nonlinear increase of chip flow velocity. |
| Moufki and Molinari [80] | Turning 42CrMo4 steel under various cutting parameters | $\mu = \mu_{0} \left(1 - \left(\frac{T}{T_{r}}\right)^{d}\right)$ | Temperature-dependent friction coefficient model as a function of the average temperature of the rake face. |
| Klocke et al [81] | Vertical broaching direct aged alloy 718 with uncoated carbide H13A | $\mu = \begin{cases} \mu_{0} & T < T_{0} \\ \mu_{0} \left(1 - \left(\frac{T_{r}}{T_{r}}\right)^{d}\right) & T \geq T_{0} \end{cases}$ | Temperature-dependent friction model taking into account the softening effect. |
| Zemzemi et al [82] | Open tribo-system measurement of AISI 4142 steel with TiN coated carbide pin | $\mu = A(V_{s})^{-B}$ | Apparent friction coefficient considering sliding velocity. |
| Outeiro et al [83] | Orthogonal turning OFHC copper | $\mu = C_{l} + \frac{C_{l} V_{k}}{1 + [(V_{k} - C_{l})/C_{l}]^{p_{l}}}$ | Apparent friction coefficient considering sliding velocity. |
| Abouridouane et al [84] | Turning AISI 1045 steel with an uncoated carbide tool | $\mu = C_{1} \cdot \exp(-\frac{1}{C_{1}}) + C_{2} \cdot \exp\left(-\frac{\alpha}{C_{2}}\right)$ | Apparent friction coefficient as the function of cutting speed and uncut chip thickness. |
| Chen et al [85] | Orthogonal turning Ti-6Al-4V with an uncoated carbide tool | $\mu = C_{1} \cdot 3.3V^{-0.474} \times 0.04f^{-0.0435} \times 0.04d^{-0.4034}$ | Regression data considered cutting speed, feed rate, and depth of cut. |
| Hao et al [88] | Turning cupronickel B10 with uncoated cemented carbide YG6 | $\mu = C_{1} \left(1 - [T - T_{C}]/(T_{m} - T_{C})\right)^{C_{1}} e^{(\alpha)} \times \left[C_{3} - C_{4} \ln(V + C_{5})\right]$ | Considering temperature, normal loads and cutting speeds simultaneously. |
| Peng et al [89] | Vertical broaching steel AISI 1045 and direct aged alloy 718 with uncoated carbide tools | $\mu = C_{1} \cdot V^{C_{1}} \times T^{C_{1}} \times \sigma^{C_{1}}$ | Apparent friction coefficient based on relative sliding velocity, contact pressure and contact temperature. |

Table 1. (Continued.)

Figure 11. Cutting force components, (a) Merchant shear model, (b) ploughing force at cutting edge. Reprinted by permission from Springer Nature Customer Service Centre GmbH: Springer Nature, Arch. Civ. Mech. Eng [34], © 2021.
stress, and normal stress [75]. These relationships were further modified using the limited friction stress and equivalent effective flow stress [76]. Several hybrid analytical approaches were proposed to identify the sticking region. As illustrated in figure 13, the empirical models of the local friction coefficient were established between the normal stress and relative slipping rate based on sticking theory, which also considered the thickness of the stagnant and shear flow layers in the transition regions [77–79].

In several studies, the influence of the cutting temperature in the friction model was considered. A temperature-dependent friction coefficient model was established in which the friction coefficient was regarded as a function of the rake face’s average cutting temperature [80]. The modified model further considered the softening effect resulting from the increase in cutting temperature [81]. The statistical fitting method was also adopted to establish the change law of the friction coefficient with various cutting parameters. In other research, the relationship between the apparent friction coefficient and sliding velocity was established [82, 83]. The evident friction coefficient as a function of cutting speed, depth of cut, and feed rate was also predicted based on cutting experiments and the classic Oxley parallel shear band model [84, 85]. Several researchers have also developed an empirical friction coefficient model considering the effects of cutting parameters and lubrication conditions [86, 87]. Moreover, a friction coefficient model was established considering cutting temperature, normal loads, and cutting speeds simultaneously during the turning process [88]. The apparent friction coefficient based on the relative sliding velocity, contact pressure, and contact temperature was also determined through the tribometer test [89].
4. Application of simulation for friction characteristics

Finite element simulation has been widely used to investigate cutting processes. Compared with traditional cutting experiments, simulation technology can save time, reduce material consumption, and obtain detailed process feature information that is difficult or impossible to measure through cutting experiments. The reliability of finite element simulation depends on the accuracy of the input parameters, and a suitable friction model is crucial for obtaining reliable results that are close to the actual cutting process. Therefore, researchers have used various friction models in the finite element simulation of the cutting process, which mainly included the constant Coulomb friction model, constant shear friction model, sticking–sliding friction model, temperature-dependent friction model and speed-dependent friction model. Table 2 lists a summary of critical research about the use of simulation for friction characteristics.

The friction behaviors in the simulation method significantly impact the cutting process. As shown in figure 14, the variations of the multiphysics distribution (cutting temperature, strain, and strain rate), cutting forces and chip formation are all affected by the input parameters of the friction model. The approach of altering the friction coefficient at the tool–chip interface was proposed to investigate the built-up edge, where a sudden increase in the local friction coefficient led to an adhesion region [36]. Similarly, the formation mechanism of a dead metal zone (DMZ) was also investigated, indicating that the size of the DMZ increased with an increase in the friction coefficient [90]. Moreover, the cutting process was simulated according to a three-phase friction model to understand the machining of Al/SiCp composites, which indicated that two-body sliding, three-body rolling, and matrix adhesion were critical factors of frictional contact interfaces [91]. Several studies also emphasized such aspects as fragment morphology and the deformation of friction conditions on chip formation during the cutting process [92, 93]. In addition, the friction law at the tool–chip interface was modified, indicating that the contact length, shear angle, cutting temperature, and cutting forces depended on the friction coefficient [94].

The various friction models had different simulation results, especially the variable friction coefficient, which reduced the error between the simulation and experimental results [95]. Figure 15 shows the simulation results of various friction models concerning the Coulomb friction model. The different friction conditions were defined at the tool–chip interface with the shear friction model and the tool–workpiece interface with the Coulomb friction model. The cutting forces and chip morphology were predicted accurately [96]. Moreover, the simulation process variables were also closest to the experimental results when variable shear friction was used in the sticking region and variable friction coefficient in the sliding region over the entire tool–chip interface [12, 97]. Several studies focused on improving the simulation accuracy using the sticking–sliding friction model. The predicted results for tool wear profiles, the relative velocity field, the cutting temperature, and the cutting forces were all close to the experiment values [29, 98]. In addition, the simulation accuracy could also be improved by incorporating the cutting process parameter variables in the friction models, including the velocity-dependent friction coefficient [30, 99], the normal force-dependent friction coefficient, the temperature-dependent friction coefficient, and various combinations of dependencies [100], which eliminated the proportional relationship between the frictional stress and normal stress allowing for more accurate prediction results [101]. Furthermore, the pressure-dependent shear friction model provided more consistent results concerning the velocity-dependent friction model for precise prediction of chip formation, contact length, and mechanical forces [102].

5. Factors affecting friction behaviors

5.1. Matching the workpiece and tool materials

Matching the workpiece and tool materials affects friction behaviors at the contact interfaces. As illustrated in figure 16, different friction behaviors were investigated by matching AISI 1046 ferrite pearlite, Ti-6Al-4V, Inconel 718, CFRP, AISI 4142 martensitic and tool materials, indicating that the apparent friction coefficient of different materials varied widely from 0.1 to 0.7 [103]. For low cutting speeds, the differences were more significant. Moreover, Inconel 718 and Ti-6Al-4V resulted in a lower friction coefficient than any steel under a dry environment [104]. The ferrite-pearlitic and austenitic steels had a more significant friction coefficient than martensitic steels. The friction coefficients of all types of steels were approximately 0.2 at a high cutting speed of 300 m min$^{-1}$ [105]. The friction behaviors during the machining of cast aluminum were also investigated with high-speed steel (HSS), cemented carbide and polycrystalline diamond (PCD) tools. The HSS and cemented carbide tools generated a high friction coefficient, whereas PCD tools appeared to have self-lubricating contacts, thereby reducing the friction coefficient [106]. The friction coefficient of PCD tools was close to a constant value compared with cemented carbide tools [107].

The cutting friction behaviors varied owing to the different physical-mechanical properties and the material microstructure of the workpiece. The friction coefficients in the machining processes of three types of high-strength compacted graphite cast iron were investigated, and the friction coefficient was inversely proportional to the hardness of the workpiece [108]. The increase in graphite refinement and spheroidization also reduced the friction coefficient. Moreover, the friction behavior depended on the microstructure of the workpiece, and the friction coefficient of Ti-6Al-4V was smaller than that of Ti-555 [109].

5.2. Cutting parameters and tool wear

The friction conditions during the cutting process are affected by cutting speed, feed rate, tool angle, and tool wear state. As shown in figure 17, the tool rake angle alters the friction conditions by controlling the contact area and the plastic
| References           | Models                                      | Conditions                                                                 | Remarks                                                                 |
|----------------------|---------------------------------------------|----------------------------------------------------------------------------|-------------------------------------------------------------------------|
| Atlati et al [36]    | Modified Coulomb friction model             | Changing local contact conditions by modifying the friction coefficient.    | The sudden increase in local friction coefficient promoted built-up edge. |
| Wan et al [90]       | Sticking–sliding friction model             | Considering the effects of friction coefficient on DMZ.                    | The size of DMZ increased with the friction coefficient increasing.      |
| Duan et al [91]      | Coulomb friction model                      | Improving the Coulomb friction model based on a three-phase friction mechanism. | Two-body sliding, three-body rolling, and matrix adhesion were critical reasons for the frictional contact interface. |
| Menezes et al [92]   | Coulomb friction model                      | Considering various friction coefficient constant.                        | The number of rock fragments decreased with the friction coefficient increasing. |
| Leopold and Wohlgemuth [93] | Coulomb friction model                  | Considering various friction coefficient constant.                         | Emphasized the effect of friction coefficient on chip formation.       |
| Shi et al [94]       | Modified Coulomb friction model             | Considering critical friction stress.                                     | Reduced errors of prediction and experiment in contact length, shear angle, cutting temperature and forces. |
| Arrazola et al [95]  | Variable friction coefficient model         | Considering variable friction coefficient at different contact regions.    | Reduced the errors between experimental and simulated results by close to 10%. |
| Oliaei et al [96]    | Coulomb friction model and shear friction model | Considering the tool–chip interface with shear friction model and tool–workpiece interface with Coulomb friction model. | Cutting forces and chip morphology were predicted accurately.          |
| Özel [12]            | Variable shear friction                    | Considering variable shear friction in the sticking region and variable friction coefficient in the sliding region. | Simulated process variables were closest to the experimental results.   |
| Arrazola and Özel [97]| Coulomb friction model and sticking–sliding friction model | Comparing the effects of the friction model on forces, stresses and temperatures. | The major shortcoming of the sticking–sliding friction model was limiting shear stress value. |
| Liu et al [29]       | Coulomb friction model and sticking–sliding friction model | Comparing various friction models on the velocity field.                   | The velocity field of the friction interface was close to the experimental results of the improved sticking–sliding friction model. Sticking–sliding friction model outperformed Coulomb friction and shear friction in predicting tool wear profiles by affecting the relative velocity, temperature and pressure. The error of cutting temperature was reduced compared to the constant friction coefficient. The error of residual stress was reduced compared to the constant friction coefficient. Eliminated the proportional relationship between frictional and normal stress allowing for accurate heat sources. |
| Lorentzon et al [98] | Shear friction model and sticking–sliding friction model | Comparing the effects of the friction model on tool wear.                   |                                                                                          |
| Mane et al [30]      | Velocity-dependent friction model           | Considering the effect of relative cutting speed.                          |                                                                                          |
| Denguir et al [99]   | Velocity-dependent friction model           | Considering the effect of relative cutting speed.                          |                                                                                          |
| Schulze et al [100]  | Variable friction coefficient model         | Considering mechanical and thermal loads in the friction properties of components. |                                                                                          |
| Zanger et al [101]   | Friction coefficient dependent variable parameters | Comparing different friction coefficient models on cutting temperature.   | Cutting temperature prediction was enhanced by the combination model of normal force and relative velocity. Pressure-dependent shear friction model and sticking–sliding friction model provided more consistent results with experimental results. |
| Malakizadi et al [102]| Shear friction model, sticking–sliding friction model, and friction-dependent variable cutting parameters | Comparing different friction models on prediction accuracy of chip formation, contact length, and mechanical forces. |                                                                                          |
Figure 14. Simulation of friction behaviors. (a) built-up edge. Reprinted from [36], Copyright (2015), with permission from Elsevier. (b) Three-phase friction of Al/SiCp composites. Reprinted from [91], Copyright (2018), with permission from Elsevier. (c) Rock fragment morphology. Reprinted by permission from Springer Nature Customer Service Centre GmbH: Springer Nature, Int. J. Adv. Manuf. Tech [92], © 2017. (d) Chip morphology. Reprinted by permission from Springer Nature Customer Service Centre GmbH: Springer Nature [93], © 2010.

deformation at the tool–chip interface. In one study, the apparent friction coefficient increased by approximately 42% when the rake angle changed from 0 to 20° [110]. Similar findings were obtained in which the interfacial friction force with a rake angle of 0° was less than that with a rake angle of 20° [109]. In addition, the critical negative rake angle was associated with the adhesive friction coefficient at the tool–workpiece interface [111]. The average friction coefficient increased and remained constant as the critical negative rake angle increased. These results are related to the change in the plastic flow characteristics.

The relative sliding velocity and contact pressure directly affect the friction state as described by the Striebeck curve. These influential parameters are embodied in the change of the relevant cutting parameters. As illustrated in figure 18, the apparent friction coefficient decreases with increased cutting speed. The friction coefficient tended to be 0.2–0.3 under high speed, and the chip bottoms generated a thin layer of softened metal to reduce friction [72, 112]. The underlying mechanism also might be that the elevated cutting speed increased the cutting temperature, which reduced the friction inside the microstructure [66]. The variation of the friction...
Figure 16. Friction coefficient vs. material matching, (a) AISI 1046 Ferrite pearlite, Ti6Al4V, Inconel 718, CFRP, AISI 4142 martensitic. Reprinted from [103], Copyright (2013), with permission from Elsevier. (b) Ti-6Al-4V and Ti-555. Reprinted from [109], Copyright (2013), with permission from Elsevier.

Figure 17. Friction coefficient vs. tool rake angle. [110] 2022, reprinted by permission of the publisher (Taylor & Francis Ltd, www.tandfonline.com).

Figure 18. Friction coefficient vs. relative velocity, (a) friction coefficient vs. cutting speed. Reprinted from [72]. Copyright (2009), with permission from Elsevier. (b) Friction coefficient as the function of sliding velocity. Reprinted from [113], Copyright (2021), with permission from Elsevier.
coefficient decreased from 0.72 to 0.35 with an increase in the sliding velocity, which resulted from the molten material at the contact interface forming a semi-solid friction state [113]. Moreover, the friction coefficient of the rake face also decreased sharply compared with that of the flank face and increased cutting speed. Such was related to the sufficient friction heat at the rake face [114].

The impact of the cutting parameters on the changing friction conditions has drawn the attention of researchers. As shown in figure 19, the cutting thickness changes the friction behaviors by affecting the specific gravity of the sticking and sliding friction, and the friction coefficient decreases at higher thicknesses [115]. The friction coefficient variations with the feed rate, tool nose radius, and depth of cut were investigated [116]. The friction coefficient increased with a larger nose radius and higher feed rates while decreasing with the increase in the depth of cut. Many researchers have also investigated the progress of tool wear under friction conditions. As shown in figure 20, the friction coefficient was divided into three characteristic cycles, increasing as the tool wear progressed [117]. The entire cycle of the friction coefficient was divided into a low friction state, plough friction state and a coating failure state during machining with coated tools [118]. Conversely, the friction coefficient decreased with an increase in tool wear, which could be related to thermal effects caused by friction behaviors [119].

5.3. Lubrication/cooling conditions

Since the application of cutting fluid is applied to avoid direct friction during solid contact, thereby reducing the friction coefficient during the cutting process [120]. Recently, high-pressure cooling (HPC) assisted machining technology has emerged for difficult-to-cut materials, improving the cutting fluid’s penetration ability into the cutting region and enhancing the lubrication effect [121]. Several studies have indicated that an HPC strategy reduced friction forces, and the friction coefficient also decreased with the increase in fluid pressure [23, 122]. However, a large amount of cutting fluid harms worker health, pollutes the environment, and has high disposal costs.
Several novel lubrication/cooling methods for sustainable machining technologies have been developed to eliminate the adverse effects associated with the cutting fluid. Minimum quantity lubrication (MQL) technology has been used widely. As shown in figure 21, the MQL method reduced the friction coefficient compared with dry cutting because the tool–chip contact length was shortened [123]. Comparative experiments for different cooling conditions (dry, MQL, and high-pressure air) were conducted, indicating that MQL was more beneficial for friction reduction at lower cutting speeds [124, 125]. However, the friction behaviors were highly dependent on the lubrication types. The application of MQL + EP/AW resulted in the formation of a thin friction film on the tool surface, reducing the friction coefficient of the rake face by 6.2%–15.4% compared with only MQL [126]. Several studies also found that using MQL with various lubricating oils resulted in different reduction effects [127]. The vegetable oils with various physicochemical properties were also analyzed, and palm oil had the lowest friction coefficient with a reduction of 17.76%–78% [128].

In addition, the combination of MQL and nanofluids further improved the load-bearing capacity of the lubricating film and reduced the friction coefficient. As shown in figure 22, the nanoparticles in the cutting fluid generate a ball-bearing effect between the contact surfaces. The lubricating ability of the mixed fluid was significantly improved when graphene oxide nanoparticles were added, which reduced the cutting friction force [129]. As illustrated in figure 23, the friction coefficient was further reduced by 51.7% when 1.5 wt.% nanographene was added to the base oil when drilling AISI 321 stainless steel as compared with only MQL [130]. In several studies, the effects of various nano-additives on lubrication were compared. The calcium fluoride (CaF₂) and molybdenum disulfide (MoS₂) solid lubricants were mixed to prepare the hybrid cutting nanofluids HN-GCF-0.3M, which reduced the friction coefficient by 11% [131]. The results of several studies also indicated that the friction film formed by Al₂O₃ nanofluids reduced the sliding friction coefficient compared with Ag nanofluids, and the mixing of multi-walled carbon nanotubes with Al₂O₃ nanoparticles further enhanced the friction reduction effects [132]. Moreover, graphene nanosheets (GPL) used as an additive reduced the friction coefficient under the electrostatic MQL technique. This was associated with the enhancement of the penetration and deposition of GPL droplets at the interfacial friction area [133].

Many scholars have provided quantitative data on the friction coefficient under cryogenic cooling conditions. Cryogenic liquid nitrogen jet cooling suppresses strong adhesion at the tool–chip interface and reduces the friction coefficient [134, 135]. As shown in figure 24, cryogenic liquid nitrogen cooling during turning Ti-6Al-4V could reduce the friction coefficient compared with dry cutting [136]. Conversely, cryogenic cooling did not diminish but increased the friction coefficient in some instances, and cryogenic cooling decreased the thermal softening of the material interface and increased the friction force [137]. Several studies also provided new lubrication
methods. When water vapor as a high-speed jet entered the cutting region to form the boundary lubrication layer, it inhibited the adhesion between the tool and chip, effectively reducing the average friction coefficient [138]. The friction coefficients under cold air cooling were 44.8% and 11.3% lower in turning stainless steel than in dry and MQL conditions, respectively [139]. Moreover, the water vapor with air cooling lubrication further reduced the friction coefficient of the tool–chip interface during the machining of Inconel 718 in contrast to dry cutting [37].

Solid lubricants are also attractive in dry cutting conditions. These lubricants commonly have a layered structure for application at the contact interface and improved friction properties. The friction coefficient of the tool–chip interface was reduced when the tools were embedded with the solid lubricant MoS$_2$ when compared with conventional tools [140]. This phenomenon is related to reducing contact length owing to self-lubricating film formation. As shown in figure 25, the self-lubrication in ceramic tools could provide a new solution to improve anti-friction properties by forming a lubrication film. The composition of the ceramic tools was optimized, where the prepared Al$_2$O$_3$/Ti(C,N)/CaF$_2$ self-lubricating ceramic tool effectively reduced the friction coefficient of the contact interface and improved the cutting performance [141]. Moreover, Al$_2$O$_3$ ceramic tools with differing TiB$_2$ contents were prepared using the hot press method, indicating that the friction coefficient of Al$_2$O$_3$/TiB$_2$ ceramic tools decreased during the dry turning of hardened steels [142]. Al$_2$O$_3$/TiC/CaF$_2$ self-lubricating tools using solid lubricant technology were also developed [143]. Several researchers reported that the functional graded self-lubricating ceramic tools had great potential for composition optimization to obtain the desired tool properties [144]. WC–TiC–Ni$_3$Al–CaF$_2$ graded self-lubricating tools appeared to have a lower friction coefficient than uniform cutting tools [145].
5.4. Micro/nano texture on the tool surface

Surface texturing on the cutting tools is a viable solution to change friction behaviors during cutting. The fabrication of micro/nano textures on the tool rake and flank faces can improve the cutting performance by reducing the contact area, trapping wear debris, reducing material adhesion, and promoting lubrication at the cutting friction interface [31, 146]. Figure 26 shows the various surface textures of the cutting tools in the application of turning, drilling, and milling processes. The textured tools reduce the friction coefficient by 14% compared with non-textured tools in orthogonal turning [147]. The milling tools with a regular array of micro-grooves improve the tool–chip interface’s tribological properties, reducing the friction coefficient and material adhesion [148]. Moreover, the micro-textured circular dimples forming on the edge side of the drilling tools reduce the friction coefficient of the contact surface by 14.29% during the drilling of Ti-6Al-4V [149].

In addition, the effectiveness of micro/nano textures on the tool surface strongly depends on the texture geometry. As shown in figure 27, four types of micro textures were fabricated on the diamond tool rake face, including straight groove arrays, concentric circles, annular sequences, and grid textures, indicating that the friction coefficients of the micro-textured tools were reduced, except for the concentric circle texture [150]. The symmetrical tapered micro groove texture showed more obvious advantages in reducing the friction coefficient at the tool–chip interface than the parallel micro grooves [151]. Xing et al [152] also compared the tool performances of different surface textures, indicating that the effect of friction reduction of the wavy texture was better than that of the linear texture perpendicular or parallel to the cutting edges. Jiang et al [153] indicated that the square pits of micro-texture had a better friction reduction effect than grooves and rectangular pits during the turning of titanium alloys with PCD tools. In addition, the friction reduction effect of the wire texture was better than the hexagonal texture under a high load,
while the opposite result was obtained with a load greater than 15 N [154].

The texture orientation also had critical effects on friction behaviors. The micro-grooves on the tool surface were fabricated with different orientations (parallel, perpendicular, or inclined) relative to the cutting edge. Using textured cutting tools reduced the friction coefficient and adhesion area. The parallel directional texture achieved the best friction reduction [148, 155]. Conversely, Gajrani et al. [156] found that the friction reduction of vertical micro-texture to cutting edges was better than that of parallel micro-textured tools. In other studies, the texture density was explored for reducing the friction coefficient, and the tool performance was improved as the density of the micro pits or grooves increased [157, 158]. As shown in figure 28, the designed isotropic areal texture was more effective in reducing friction than the discrete linear texture [159]. The nano-scale texture on the rake face was more effective than the micro-scale texture in reducing friction and adhesion [160]. Moreover, the correct placement of the texture relative to the cutting edge determined the cutting performance, and placing the texture 100 µm from the cutting edge to reduce friction was the most effective [161].

The hybrid texture was also effective in reducing the friction coefficient. As illustrated in figure 29, the friction coefficient of a hybrid textured tool with the combination of circular dimpled holes and linear grooves was smaller than that of a single textured tool [162, 163]. The friction coefficient of the hybrid textured tool was approximately 16% lower than that of the conventional tool. Such results indicated that the hybrid texture facilitated the formation of a complete lubricating film at the tool–chip interface [164]. Moreover, the friction performance of the hybrid textured tool was better than those of variable density texture and variable shape texture [165].

The synergistic mechanism combining the surface texture and lubrication strategy further improved friction behaviors. As shown in figure 30, applying a micro surface texture resulted in better infiltration of the cutting fluid at the tool–chip interface, which fortified the lubrication film and increased the cooling effectiveness [166]. As shown in figure 31, MQL combined with micro-textured tools could improve the anti-wear performance and the oil film load carrying capacity, further reducing the friction coefficient. The friction reduction of the micro-texture tool with sufficient lubrication was more significant than that of one with insufficient lubrication [167]. Adhesive wear and the friction coefficient were reduced at the tool–chip interface when a lubrication pad was formed with interconnected macro channels and micro-texture [168]. Moreover, the friction coefficients were reduced by 11.5% and 10.9% when applied to the textured surface with composite lyophilic/lyophobic wettability compared to a conventional tool [169].

Several researchers combined micro surface textures with the solid lubrication strategy to improve tool performance during dry cutting. As shown in figure 32, the chip flow over the MoS2-coated micro-textured tools and the solid lubricant releases them, forming a tribofilm with low shear strength. The contact length and friction coefficient were reduced at the tool–chip interface [156]. Moreover, the hybrid textured tool with CaF2 solid lubricant was fabricated and presented lower friction reduction during the machining of 4340 hardened steel [170]. In addition, combining a WS2/Zr soft coating and nano texture on the tool surface further reduced the friction coefficient and improved the cutting performance [171].

5.5. Tool coatings

Various coatings are frequently deposited on cutting tools in manufacturing due to their superior wear resistance. The tool coatings prevent direct contact between the workpiece and tool substrate, making friction behaviors different from uncoated tools [172]. Research on tool coatings has mainly focused on chemical components, the preparation process and structure design. Physical vapor deposition (PVD) and chemical vapor deposition (CVD) coating techniques are widely applied to improve tool performance.
Several studies evaluated the effects of tool coatings on the interfacial friction coefficient using tribometers and cutting experiments. The tribological response originated from the substrate/coated system interaction, where the coatings controlled the contact length and reduced friction [173]. As shown in figure 33, tool coatings reduced the friction coefficient during the sliding process [174]. The application of TiAlCrN coating reduced the friction coefficient by 10%–16% under different cutting speeds during the dry turning of AISI 1045 [175]. Bar-Hen and Etsion [176] also investigated the turning performance of TiAlN coatings with various thicknesses, indicating that the optimal coating thickness of 3.5 µm maximized the wear resistance. Furthermore, friction behaviors were significantly influenced by the tool coatings’ surface integrity. The smooth surface of Al2O3 coating reduced the sliding friction coefficient relative to the rough surface [177]. Chang et al [178] also evaluated TiAlN coated tools with various surface integrities using micro-blasting, indicating that coatings with low surface roughness, medium surface hardness, and compressive residual stresses effectively reduced the friction coefficient.

The properties of tool coatings are primarily determined by their chemical compositions. As shown in figure 34, the different friction behaviors of AlCrN and AlTiN coatings mainly resulted from the other debris removal behaviors [179]. The co-doping of carbon (C) and boron (B) improved the friction resistance for AlTiN coatings compared with doping alone, so that AlTiBCN > AlTiCN > AlTiBN > AlTiN [180]. AlTiN and AlCrN coatings with high Al content exhibited better friction resistance than TiN and TiAlN coatings [181]. The friction coefficient of AlCrN coating against Ti-6Al-4V under low load conditions was lower than that of AlTiN coating, while the friction properties were similar under high load conditions [182]. In addition, the coating peeling was suppressed by...
designing a Si-containing gradient structure, indicating that an AlCrSiN–C coating with a smoother Si content gradient had the lowest friction coefficient [25].

Since the multi-layer coating structure helps improve tool wear resistance, tool coating technology has evolved from the early single-layer coating to multi-layer versions incorporating thin films [183]. As shown in figure 35, the friction coefficient of the silicon-containing multi-layer coatingAITiN + AITiSiN + AITiSiN/TiSiN + TiSiN was lower than those of AITiN and AITiN + AITiSiN coatings [38]. Several researchers suggested the use of nano-structured coatings. The friction properties of high-quality multi-layer nano diamond gradient microcrystalline diamond/ nanocrystalline diamond/ ultrananocrystalline diamond (MCD/NCD/UNCD) coated end mills exceeded those of single-layer coatings [184]. In addition, the effects of the nanolayer thickness in Ti–TiN–(Ti,Al,Cr)N coatings on the friction properties were also evaluated, indicating that the friction coefficient appeared as a non-monotonic variation and a nanolayer thickness of 16 nm provided the lowest friction coefficient [185].

Studies have indicated that tools with hard coatings have the advantage of high wear resistance, while tools with soft coatings have the advantage of a low friction coefficient. The hard/soft composite coating tools might combine both advantages. As shown in figure 36, the CrCN–WS₂ hard/soft composite coating deposited on the cemented carbide tool surface had better friction performance than the CrCN-only and WS₂-only coatings [186]. In addition, the TiAlN/WS bilayer thickness was designed for forming coatings with a self-lubricating effect and high wear resistance [187].

6. Consequences of friction behaviors

6.1. Tool wear patterns

Tool wear is inevitable in industrial manufacturing, strongly controlled by local interfacial friction phenomena. Since severe friction generates high thermo-mechanical load gradients, the thermo-physical properties of the tool materials near the contact interface would be destroyed [188]. Due to the prevalence of tool wear, several researchers have studied the tool wear mechanisms during various cutting processes.
Figure 37 shows the wear topographies of different cutting tools after machining, indicating that the rake face and the flank face had severe friction marks. A large amount of material adhesion and partially exposed substrate appeared on the tool surface [189]. The tool wear patterns mainly included crater, flank, and cutting edge wear. The tool wear characteristics of different wear regions appeared diversity because of the various friction conditions. Several studies also have demonstrated that tool wear was associated with critical factors, including physical processes, chemical reactions, and thermo-mechanical phenomena caused by local cutting friction behaviors [32]. In addition, the tool wear mechanisms at low cutting speed were mainly mechanical and adhesive wear caused by friction. In contrast, diffusion wear and oxidative wear were dominant owing to different levels of heat generation by the cutting friction [190].

In addition, the progressive tool wear caused changes in tool geometry morphology, as shown in figure 38. The rake face was mainly crater wear, while the flank face was mostly flat wear [191]. The crater wear was also exacerbated by the gradient thermo-mechanical loads generated by the cutting friction [192, 193]. The evolution of crater wear was also non-linear [194]. Because of the cumulative effect of frictional heat sources at the tool–chip interface, the maximum crater wear depth was situated at a certain distance from the cutting edge.

Moreover, the plastic deformation of the tool coating is caused by friction between the physically or mechanically related adhesive material and coatings. As shown in figure 39, friction behaviors at the tool–chip interface resulted in the plastic deformation of the tool coatings in the direction of the chip flow [195, 196]. The mechanical damage to the cutting tools indicated that friction behaviors led to tearing and micro-cracks of WC–Co particles, as well as creep deformation [191]. Such results were related to the rearrangement between the substrate WC and the binder Co. Several researchers have used lubrication/cooling, surface texture, and tool coatings to reduce the friction coefficient. As shown in figure 40, the cross-sectional profiles of the cutting tools indicated that the crater wear with the micro-textured tool was much lower than that with the conventional tool [197].
6.2. Tool life

Cutting friction behaviors result in high temperature, strong adhesion, and high wear rates, directly reducing tool life. Specifically, tool life at a high cutting speed is shortened by many times compared with a low cutting speed [198]. This further limits the application of high-speed machining technology. Since the tool wear rate depends mainly on the friction conditions, tool life can be improved by reducing the friction coefficient during the cutting process. In some studies, an adjustment strategy for the staged cutting speed was adopted to control the generation of interfacial nano-scale tribofilms, which prolonged the tool life by 90% during the turning process [199]. As illustrated in figure 41, the effects of dry cutting, cryogenic cooling, high-pressure cooling, MQL, nanofluid lubrication, air cooling, and oil mist lubrication on the tool wear have been investigated, indicating that the cooling strategy could properly prolong tool life [200–205].
Moreover, tool life was further prolonged by more than 80% by optimizing the position of the high-pressure coolant nozzles [206].

The application of advanced tool coatings also prolongs tool life, and different types of coating have various effects. As illustrated in figure 42, the tool life of PVD AlTiN coated inserts was almost twice that of CVD TiCN + Al2O3 coating during the machining of super duplex stainless steels [207]. Adding Si element to TiAlN coating improved the tool life. In particular, the gradient TiAlSiN coating increased it by 75.4% [208]. Moreover, double-layer and multi-layer diamond films have longer tool life than single-layer diamond films. In particular, the tool life of the multi-layer film (MCD/NCD/MCD/NCD) was increased by 7.5 times [209]. Fox-Rabinovich et al [210] also found that ceramic inserts (Al2O3 + TiC) and coated carbide tools with an intermediate ceramic (Al2O3) layer presented better tool life than polycrystalline cubic boron nitride (PCBN) tools. Hao et al [211] investigated which tool life lasted the longest for a Cr/W-DLC/DLC composite coating compared with cutting Al–Si alloys. The results were 1.06, 1.15, and 1.35 times those of Cr/CrN/DLC, DLC single-layer, and uncoated tools.
Hybrid enhancement methods can further suppress the tool wear rate. As shown in figure 43, the combination of cryogenic cooling, PVD coatings and suitable cutting parameters provides a tool life that is as much as 6 times longer [212]. The mixed conditions of MQL and nanocomposite coatings can further prolong the tool life [213]. In addition, tool wear is also suppressed by the combined effects of cooling lubrication and micro textures. The method of combining high-pressure cooling with micro textures was adopted to improve tool life by more than 57.1% [214]. The self-lubricating molybdenum disulfide (MoS₂) tool with an oval groove texture on the rake face prolonged the tool life by more than 40% [215].

### 6.3. Chip formation

The friction behaviors at the tool–chip interface induce the gradient distribution of thermo-mechanical loads, which directly affect the chip formation process. Research has indicated that the thermo-mechanical loads induced by the cutting friction in the secondary shear region were the main driving factors for the evolution of the plastic behaviors of the chip bottom [216]. Figure 44 presents the friction patterns of the chip bottom under different cutting conditions. The elongated dimples along the chip flow direction appeared on the chip bottom after cutting [15, 217]. Such topography was mainly
related to plastic deformation resulting from the cutting friction of the tool–chip interface. It was also observed that the plastic deformation dimples were periodic regions, considered periodic fractures due to the sticking–sliding of chip movement on the tool rake face. Moreover, the chip bottom contact mode changed from steady sliding to sticking–sliding with elevated cutting speeds. The sticking–sliding phenomenon on the chip bottom was associated with thermodynamic instability and dynamic interaction caused by nonlinear friction and shear dissipation [39, 218].

The thermal-mechanical coupling effects of the cutting friction cause the gradient change in the microstructure of the chip bottom. Figure 45 displays various microstructure characteristics of the chip bottom under different cutting conditions. The grain boundaries of the chip bottom disappeared within a large number of refined and elongated microstructures parallel to the cutting speed direction. Such fibrous features occupying the second shear region were attributed to the strong frictional heat generation, leading to softening chip bottom material [15, 219]. Several studies also found that the microstructure of the frictional shear zone exhibited extremely refined dynamic recrystallized grains with sizes of 80–300 nm, which increased in density closer to the chip bottoms [220]. The secondary shear bands, in particular, were characterized...
Figure 45. Microstructure characteristics of the chip, (a) fibrous features occupying the second shear region. Reprinted by permission from Springer Nature Customer Service Centre GmbH: J. Mater. Res. Tech [15]. © 2020. (b) Refined dynamic recrystallized grains. Reprinted from [220], Copyright (2016), with permission from Elsevier. (c) Twins deformation with progressive tool wear. Reprinted from [33], Copyright (2018), with permission from Elsevier.

by localized white layers with thicknesses ranging from 10 µm to 30 µm in the microstructure of the AISI 1045 continuous chip [221]. The thickness of the plastic deformation layer at the chip bottom can be reduced by using the cooling/lubrication method because of the weakening of the friction effects [168]. Moreover, the chip grains were highly refined with CVD coating, whereas PVD coating produced more uniform plastic deformation. This was caused by the lower friction angle and more significant chip compression under the condition of CVD coating [222]. It also was found that the deformation mechanism of the chip changed with the degree of tool wear, where severe friction at the end of the tool life resulted in an increasing number of twins through tension and compression [33].

6.4. Machined surface

The thermo-mechanical loads resulting from the cutting friction of the tool–workpiece interface are a decisive factor affecting surface integrity. In particular, the tool wear state directly changes the tool geometry and strengthens the friction effect. As a result, the interfacial friction changes the stress state and heat distribution, which affects the surface topography, microstructure of the subsurface layer, and mechanical properties [223]. The friction effects on the surface integrity characteristics were analyzed to provide references for surface quality controlling in actual production.

The cutting friction state has a decisive influence on the surface topography. The tool wear significantly impacts the surface roughness concerning the cutting parameters (cutting speed, depth of cut and feed rate) and lubrication method. Several studies found that the friction effect induced by tool wear increased surface roughness, especially with uncoated cemented carbide tools [224]. In addition, surface roughness declined and then increased as the tool wear progressed [225]. Conversely, several researchers investigated that the machined surface roughness showed a downward trend when the tool approached the failure standard condition. The friction of the worn flank surface played a ‘wipe’ effect on the surface topography by removing the feed trace’s peaks, which led to surface roughness reduction [226]. Based on the microscale observation, the surface topography was uneven with complex defect characteristics. In particular, the frequency of surface defects increased significantly when tool wear exceeded 0.15 mm [227]. As shown in figure 46, the probability of surface defects, such as surface tearing, material smearing, grain pulling, and surface burning, increased because of the enhanced thermo-mechanical loads of the friction effects [228–230].

The cutting friction of the tool–workpiece interface generates the gradient distribution of thermo-mechanical loads, which causes plastic deformation with the difference from the substrate [231, 232]. As shown in figure 47, the surface and
subsurface materials underwent plastic deflection in the cutting direction during the cutting processes (such as turning, milling, and drilling), indicating that the thermo-mechanical loads induced by the friction effect were critical factors for local plastic behaviors [233, 234]. In particular, severe thermo-mechanical loads under tool wear conditions resulted in more profound plastic deformation [15]. Several researchers also investigated the microstructures while applying different cooling methods, such as drying, air cooling, and high-pressure cooling, indicating that a lubrication/cooling strategy could reduce plastic deformation [204, 235]. In addition, the thermo-mechanical loads caused by the cutting friction changed the
grain size, and the local deformation layer created a nanocrystalline structure [236, 237]. Additional thermo-mechanical loads resulting from tool wear further promoted the generation of white layers, as shown in figure 48 [238, 239]. The white layers consisted of numerous nano-scale equiaxed grains, which resulted from the melting and rapid cooling during the cutting process [240]. At the same time, the effect of cutting friction on the surface texture orientation was reported by some studies [241]. As illustrated in figure 49, the surfaces produced a shear texture because of the thermo-mechanical friction loads, and the texture intensity revealed the gradient change along the depth direction.

The combined effects of hardening induced by mechanical loads and softening induced by thermal loads ultimately determine the surface microhardness. As shown in figure 50, the mechanical loads caused by the cutting friction effect played a dominant role, while the increased plastic deformation and dislocation density resulted in enhanced microhardness [242, 243]. In addition, several researchers indicated that the thermal softening effect of high cutting temperature exceeded the mechanical loads-induced hardening effect in the tool wear state. The microhardness of the local surface was smaller than that of the substrate. In particular, when machining titanium alloys with low thermal conductivity, the cutting heat from the friction effect quickly accumulates on the machined surface resulting in a severe thermal softening effect [244].

The residual stress of the machined surface is mainly related to the inhomogeneous plastic deformation caused by the mechanical loads, thermal gradient, and phase transformation during the cutting process. The final residual stress state depended on the thermo-mechanical loads resulting from the cutting friction, where the thermal loads tended to produce residual tensile stresses. Still, the mechanical loads contributed to the formation of residual compressive stresses [223]. Especially under tool wear state, the machined surface was subjected to more thermo-mechanical loads, directly affecting the generation of residual stresses. On the one hand, the residual compressive stress increased when the mechanical effects exceeded the thermal effects with the progress of tool wear [243, 245]. On the other hand, the thermal effects caused by the elevated cutting temperature were more significant than the mechanical effects, which caused the residual stress state to tend toward tensile stress [233].

7. Conclusions

(a) The cutting friction behaviors depended on the thermo-mechanical loads and intense deformation during the cutting process, which was regarded as the output response variable under extreme conditions such as elevated temperature, large strain, high strain rate, shear deformation, the sticking–sliding contact state, and diverse cutting conditions. The friction heat of the cutting process resulted in severe high-temperature conditions at the contact interface. The materials near the friction interface were in an unsteady plastic flow, which was characterized by large local plastic strain and strain rate. The contact conditions changed with increased distance from the cutting edge, where the balance between sticking and sliding was destroyed by the diverse friction conditions.

(b) The critical goal is to describe the friction behaviors at the complex cutting contact interface, specifically an accurate friction model that can be embedded in finite element analysis. Various friction models were proposed by researchers to analyze the friction behaviors based on an in-depth understanding of the cutting contact state, mainly including the constant friction coefficient model, constant shear friction model, sticking–sliding friction model, temperature-dependent friction model and cutting parameters-dependent friction model. The friction behaviors in the simulation had an impact on the multiphysics distribution (cutting temperature, strain and strain rate), cutting forces, and chip formation, demonstrating that different friction models could directly provide diverse results.

(c) Many factors affected the cutting friction behaviors by directly or indirectly changing the interface contact conditions, which included material matching for the workpiece and cutting tool, the cooling medium and lubrication method in the cutting contact region, the optimization...
of the cutting parameters, and modification of the contact interface through micro/nano texture surface technology and tool coating technology.

(d) The harsh friction behaviors in the cutting process resulted in localized thermo-mechanical loads at the contact interface, inevitably causing tool wear processes and shortened tool life. Moreover, the friction conditions at the tool–chip interface were the main driving factors for the evolution of the friction patterns and microstructure of the chip bottom. In contrast, the friction conditions at the tool–workpiece interface affected the surface topography, surface microstructure, and mechanical properties. Specifically, the tool wear states changed the tool geometry and strengthened the friction effect.

8. Research limitations and future work

Currently, there are many unsolved issues and considerable potential for improvement. Industry needs for process indicators will primarily drive future research on cutting friction behaviors. The research limitations and potential future work are as follows:

(a) Since most of the current friction models depend on the friction force or a single friction coefficient, future friction models must consider comprehensive factors, such as interface geometry, material properties, lubrication conditions, and thermo-mechanical loads. The simulation accuracy also must be evaluated. The cutting friction database should be established with connections between the friction behaviors and cutting requirements. This should help guide tool selection and cutting process optimization based on friction theory.

(b) Most friction data acquisition equipment is designed to focus on a specific aspect of the friction problem, which has many unsolved problems, such as loading control or sample preparation. Advanced friction acquisition equipment is necessary for the design and the conversion of the input of traditional friction parameters to realistic cutting conditions. Such equipment would evaluate various cutting speeds, pressures, temperatures, lubrication states, and materials/coatings. In addition, advanced technologies must be developed to capture the cutting friction behaviors, especially the experimental methods that can separate the mechanical, adhesion and ploughing components under diverse friction conditions.

(c) Researchers currently use multiple cutting experiments to determine the optimal parameters to reduce friction under various machining conditions. Further understanding of the influences of tool coatings, cutting fluid, and tool wear on friction behaviors is needed, and the friction behaviors should be analyzed while considering multiple factors.

(d) Based on studying the friction characteristics during machining difficult-to-cut material, it is necessary to analyze the tool wear processes under the influence of thermo-mechanical coupling fields. This should enable high-speed cutting technology to advance by increasing the predictability of tool wear states and tool life.

(e) Further research should focus on the effects of friction behaviors on surface integrity, especially with progressive tool wear. Using single surface integrity parameters to judge tool life should be avoided because the need to establish the tool life criteria is based on multi-objective optimization of surface integrity. Moreover, the relationship between material processing-surface integrity-service performance associated with metal cutting should be considered.

(f) Future work should focus on cutting friction mechanisms under extreme machining methods, such as ultra-high speed cutting, ultra-precision cutting, heavy-duty cutting, ultra-low temperature liquid nitrogen cooling-assisted cutting, etc.

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Conflict of interest

There are no conflicts of interest for any authors.

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