The relationship between adhesion morphology and cutting force in orthogonal cutting of 6061-T6 aluminum alloy

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Received: 26 January 2022 / Accepted: 23 November 2022 / Published online: 6 December 2022
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Abstract
Due to the sticky and soft characteristics of aluminum alloy, it is easy to form adhesion on the tool rake face during cutting process. In this work, the orthogonal cutting tests for 6061-T6 aluminum were carried out at cutting speeds of 20–300 m/min and feed rates of 0.05–0.3 mm, and the relationship between adhesion and cutting force has been explored. A slip-line model was used to separate the edge force of cutting edge to obtain the pure friction force of tool rake face, thereby obtaining the average friction coefficient. Adhesion morphologies were observed under the scanning electron microscope (SEM) and compared with the average friction coefficients. It was found that different adhesion morphologies correspond to different friction coefficient ranges. Simultaneously, the relationship between the temperature and normal stress on the tool rake face and the adhesion morphology and friction coefficient was investigated. The results showed that the temperature is the major factor of adhesion evolution and friction coefficient change. Combining the tool rake face temperature and the adhesion morphology, it was noted that the reduction of the friction coefficient is not only owing to the self-lubrication caused by thermal softening, but also due to the decrease of the total shear force caused by the incomplete contact. Finally, the relationship between the friction coefficient of tool rake face and the cutting force and specific cutting energy was discussed, with the conclusion that the friction coefficient has an impact on the cutting force and specific cutting energy by changing the shear angle.

Keywords 6061-T6 aluminum alloy · Adhesion · Friction coefficient · Cutting force

1 Introduction
The adhesion problem of ductile metal materials in the cutting process has long been a research topic, which includes the formation mechanism of adhesion, the influence of adhesion on the friction coefficient, and the adhesion law under different cutting parameters. Although a unified model for the adhesion process of tool rake face and its effect has yet to be established, several findings have been validated and acknowledged by the majority of researchers. Zorev [1] discovered that there are two contact zones on the tool-chip interface during the cutting process: the sticking zone and the sliding zone, and the normal stress of tool-chip interface decreases gradually from the tool edge to the tool-chip separation point along the tool rake face, and the normal stress at the tool edge should approach infinity.

Based on Zorev’s dual contact zone theory, many researchers have undertaken extensive research on the friction behavior of the tool-chip interface. Childs [2] thought that the ratio of normal stress to friction stress in the sliding zone of the tool-chip interface is not constant, and proposed a new friction model based on the relationship between the friction coefficient and the strain rate by comparing numerical simulation and experiment. In his model, the friction coefficient rises as the strain rate rises. Ozlu et al. [3] measured the sliding friction coefficient with a modified pin-on-disc setup. The results were consistent with the sliding friction coefficient obtained from the cutting experiment, and it was proposed that friction speed is the primary influencing factor of the sliding friction coefficient. Behera et al. [4] studied the friction behavior under minimal quantity lubrication (MQL), and developed a friction model based on cutting speed and MQL parameters, which predicted the experimental results of cutting force, total tool-chip contact length, and adhesion length with good consistency. Other researchers [5, 6] also suggested that the sliding friction coefficient is mostly connected to
the friction speed. In addition, based on a new friction test method, Puls et al. [7] suggested a temperature-dependent friction model. To characterize the influence of temperature on friction, this model adopted a concept comparable to the thermal softening effect in the material constitutive model. They developed a finite element model (FEM) to precisely obtain the temperature distribution of the contact interface in the test and utilized the temperature of the contact surface to identify the parameters in the model. Peng et al. [8] has utilized Puls' approach to develop friction models between different tools and materials. Furthermore, they considered that the friction coefficient should be related to temperature and stress. Thus they introduced Klocke's [9] model based on temperature, pressure, and speed. On the other hand, they noted that Klocke's friction model is based on sheet forming, which may differ from the friction mechanism of the tool-chip interface in cutting.

Some researchers have also come up with varied conclusions of adhesion formation mechanism of aluminum alloy and its impact. The traditional view is that the tool-chip contact near the tool edge is a complete contact sticking zone due to higher normal stress and the tool-chip contact near the tool-chip separation point is an incomplete contact sliding zone due to the lower normal stress. Song et al. [10] suggested that adhesion is first formed in the middle of the tool-chip interface, which is conducive to the formation of the initial adhering layer. As the cutting continues, it finally grows as a built-up edge (BUE). And they proved that the formation of adhesion is mainly affected by temperature. When Gomez-Parra et al. [11] used TiN-coated tools to process 7075-T6 aluminum alloy, it was found that the formation of BUE is due to the formation of an adhering layer at the initial moment of the cutting process. The initial adhering layer changes the microstructure and geometry of the tool rake face, which is conducive to the formation of BUE. They also held the view that the formation of adhesion is related to temperature. In terms of the influence of adhesion, Gomez-Parra et al. [11] discovered that as the BUE increased, the surface roughness of the machined surface decreased. In response to this phenomenon, Batista et al. [12] pointed out that this is because the formation of a BUE changes the tool geometry. The degree of adhesion increases as the cutting time grows, which influences the tool geometry and, in turn, causes the machined surface roughness to decrease.

In this paper, cutting force and adhesion morphology were related together through cutting temperature and friction coefficient, which revealed the coupling relationship between them. Firstly, the slip-line model was utilized to separate the cutting edge force and the average friction coefficient of the tool rake face is obtained, which was validated by the finite element method. Then, the adhesion morphologies of different cutting parameters observed under the SEM were compared with the average friction coefficients, and the relationship between the adhesion morphology and the friction coefficient was presented. The temperature and normal stress of the tool-chip interface were calculated by the analytical model, and the key factor of the formation of adhesion and the evolution of the average friction coefficient was discussed. Finally, the equation of the average friction coefficient and the cutting force coefficient was established by the slip-line model, and the influence mechanism of average friction coefficient for cutting force was stated. Simultaneously, the influence mechanism of average friction coefficient versus cutting energy was studied.

2 Materials and methods

The orthogonal cutting model is the simplest cutting model, and it is also the basic model that can reflect all kinds of complex cutting. Turning a tube with a 90° tool cutting edge angle is a commonly used method to simulate orthogonal cutting. Although this method is different from the real orthogonal cutting, it has been accepted and exercised by most researchers. Based on the CAK3665 CNC lathe to carry out the orthogonal cutting experiment, a 6061-T6 aluminum tube with a diameter of 58 mm was regarded as the workpiece to be machined. The average thickness of the tube is 2.15 mm. The tool holder type is STFCL1616H11, and the tool type is TCMW110204. Figure 1a shows the experimental device, while Fig. 1b shows the orthogonal cutting diagram. The tests used the coated tool YBD151 produced by Zhuzhou Cemented Carbide Company and the uncoated tool HTI10 produced by Mitsubishi Company. As is shown in Fig. 2, the YBD151-coated tool is composed of TiN, Al2O3, and TiCN, whose thickness are 1.2 μm, 6.4 μm, and 7.4 μm, respectively, and its radius of the cutting edge is 30 μm; the HTI10 uncoated tool radius of the cutting edge is 16 μm. The international standard code of HTI10 is K10. The dynamometer is Kistler 9257B, which uses three channels to measure the force in the x, y, and z directions. Table 1 is the thermophysical parameters of the tool and the workpiece.

The cutting tests were performed at the feed (uncut chip thickness) 0.05 mm, 0.1 mm, 0.2 mm, and 0.3 mm and the spindle speed 109.8 r/min, 274.4 r/min, 439 r/min, 548.8 r/min, 1097.6 r/min, 1646.4 r/min (20 m/min, 50 m/min, 80 m/min, 100 m/min, 200 m/min, 300 m/min), and the test parameters are shown in Table 2. There are 18 groups of test, each with fresh cutting tool and total cutting length of 2 m. In order to simulate orthogonal cutting as much as possible, the tool nose was not involved in cutting. The experimental design has the following assumptions: (1) the adhesion of the tool rake face is formed in a short time and does not change as the cutting length increases; (2) the influence
Fig. 1  a Experimental device, b orthogonal cutting diagram

Fig. 2  a Cutting edge radius of coated tool, b cutting edge radius of uncoated tool, c thickness of coating
of the cutting speed difference between the inner and the outer diameters on the cutting force is ignored; (3) during the cutting process, the tiny lateral flow of chips is ignored, and it is considered that it has no effect on the cutting force. Gomez-Parra et al. [11] pointed out that the adhering layer is formed within a short period of time after the start of cutting. According to the experimental results, the cutting force is basically stable throughout the cutting process, as shown in Fig. 3. Since the cutting length of each test is the same, the adhesion morphology can be used as a function of the cutting parameters. And because of the stability of the cutting force, the adhesion morphology can reflect the change trend of friction coefficient.

### 3 Results and discussion

#### 3.1 Experimental results

The cutting force and thrust force were obtained by filtering the original cutting force data and selecting a relatively stable section. The tool-chip contact length was measured under an optical microscope, and the chip thickness was measured by a spiral micrometer. Figure 4a and b can be seen that with the increase of cutting speed, the cutting force and thrust force show a trend of first increasing and then decreasing at low feed rate, and only a decreasing trend at high feed rate. As can be seen in Fig. 5a, the chip thickness change is also the same as the cutting force. Figure 5b shows that the chip contact length decreases with the decrease of the cutting speed under different feed rates. Moreover, coated tools have a significantly smaller chip thickness and contact length than uncoated tools. Thinner chips mean larger shear angles, smaller shear surface areas, and smaller cutting forces. However, it can be seen from Fig. 4 that the cutting force of the coated tool is not significantly lower than that of the uncoated tool. This is because the cutting edge radius of coated tool is larger than the uncoated tool, and the ploughing force is higher.

#### 3.1.2 Adhesion morphology

The friction coefficient of tool-chip interface is intimately related to the adhesion morphology of tool rake face. The
adhesion morphologies of coated and uncoated cutting tools with different cutting parameters were observed by optical microscope and SEM, and compared with the change of friction coefficients. As the cutting speed and feed rate rise, the adhesion of the tool rake face is classified into four types for coated tools: (I) at low feed rates and low cutting speeds, adhesion only occurs at a distance from the cutting edge, as shown in Fig. 6a; (II) as the cutting speed or feed rate increases, adhesion that fully covers the tool rake face is produced, as shown in Fig. 6b; (III) when the cutting speed or feed rate is increased further, a fully covered adhering layer and a partially covered adhering layer coexist, with the fully covered adhering layer being close to the cutting edge, as shown in Fig. 6c; (IV) when the cutting speed reaches 300 m/min, only a partially covered adhering layer is present, as shown in Fig. 6d. The adhesion morphology of the tool rake face on uncoated tools likewise has four types: (I) at low feed rate and low cutting speed, adhesion occurs at a distance from the cutting edge, as shown in Fig. 7a; (II) when the cutting speed or feed increases, an adhering layer fully covering the tool rake face appears, as shown in Fig. 7b; (III) when the cutting speed or feed rate is increased further, a fully covered adhering layer and a partially covered adhering layer coexist, as shown in Fig. 7c. Unlike coated tools, the partially covered adhering layer is close to the cutting edge; (IV) when the cutting speed is increased further, the adhesion layer is only partially covered, as seen in Fig. 7d.

When Ozlu et al. [3] studied the processing of AISI 4340 steel with coated and uncoated tools, the fully covered adhering layer was regarded as the sticking zone, and the partially covered adhering layer was regarded as the sliding zone. Bahi et al. [13] identified the delamination zone far from the cutting edge as the sliding zone when cutting AISI
1050 steel. As a result, full coverage adhesion and partial coverage adhesion are the classic sticking and sliding zones. However, the dual contact zone theory cannot explain the adhesion away from the tool edge in Fig. 6a and Fig. 7a, nor the phenomenon that the adhesive zone is away from the tool edge in Fig. 7c.

3.2 Friction coefficient of tool rake face

Most researchers neglected the effect of cutting edge on cutting force when investigating the friction coefficient of the tool rake face. Molinari et al. [14] analyzed the apparent friction coefficient $\mu_{\text{app}}$, which is the ratio of the thrust force to the cutting force, and the average friction coefficient $\bar{\mu}$, which is the ratio of the tool rake face friction stress to the normal stress. The results demonstrate that $\mu_{\text{app}}$ is slightly larger than $\bar{\mu}$, and the value of $\mu_{\text{app}} - \bar{\mu}$ rises as cutting speed rises. The ratio of the cutting edge radius to the uncut chip thickness of the coated tool is 0.6 for the 0.05-mm feed rate, while the ratio of the uncoated tool is 0.32. Therefore, in order to calculate the average friction coefficient of the tool rake face more accurately, the edge force during cutting should be separated.

Figure 8 shows the force distribution diagram along the tool rake face and the cutting edge, where point C is the tool-chip separation point, and the line CB is the contact area between the tool rake face and the chip. The point S on the tool edge BA is the material separation point. The workpiece above point S flows into the chip, and the workpiece below is squeezed onto the machined surface. The resultant force acting on the chip along the BC of the tool rake face is $Q$, and $Q_c$ and $Q_t$ are the component forces in the cutting and thrust directions, respectively. Along the tool edge BS, the resultant force acting on the workpiece is $F_{p1}$, the resultant force of normal stress and friction stress are $F_{np1}$ and $F_{fp1}$, and the resultant force of the component forces in the cutting and thrust directions are $F_{cp1}$ and $F_{tp1}$, respectively. Along the tool edge SA, the resultant force acting on the workpiece is $F_{p2}$, the resultant force of normal stress and friction stress are $F_{np2}$ and $F_{fp2}$, and the resultant force of the component forces in the cutting and thrust directions are $F_{cp2}$ and $F_{tp2}$, respectively. Therefore, the edge force is the resultant force of $F_{p1}$ and $F_{p2}$.

Albrecht [15] regarded the edge force as the ploughing force, and proposed a zero feed extrapolation method to
determine the ploughing force. This method of determining ploughing force is adopted by many researchers. Considering the ploughing force obtained by the extrapolation method, Guo and Chou [16] corrected the flow stress in the primary shear zone (PDZ) and compared it with the flow stress obtained by the constitutive model from the compression test. The result showed that the corrected flow stress is more consistent with the flow stress predicted by constitutive equation than the uncorrected one. As a result, Guo and Chou [16] asserted that extrapolation method is a reliable method for calculating ploughing force. Seif et al. [17] extracted the HCP Zerilli-Armstrong constitutive parameters of the magnesium alloy AZ31B based on the extrapolation method and the Oxley parallel shear zone model. The stress–strain curve generated by the constitutive model was consistent with the stress–strain curve obtained by Split-Hopkinson tension bar test. However, other researchers, such as Stevenson [18] and Popov and Dugin [19], pointed out that the ploughing force calculated using the extrapolation approach would be higher than the actual ploughing force. As seen in Fig. 3, Stevenson [18] argues that the dwelling force noticed when the feed is stopped might be considered as the zero feed force, or ploughing force. Popov and Dugin [19] determined the ploughing force by the comparison method of total forces at different flank wears. In fact, the extrapolation method often lacks theoretical support and makes clear errors in assumptions. For example, the zero feed extrapolation method is based on...
the assumption that the cutting force increases linearly with the feed. If the shear angle and the shear stress in the PDZ remain unchanged with the increase of the feed rate, the zero feed extrapolation method is reasonable. However, the shear angle and the shear stress in the PDZ both change with the increase of the feed rate in the cutting process.

Unlike the extrapolation method, which considers the ploughing force to be a parasitic force, the slip-line method considers the ploughing force to be the force acting on the minimal uncut chip thickness below the material separation point, that is, $F_{p2}$. In terms of the material separation point, there are two different theories. One is based on the fact that a dead metal zone (DMZ) exists, which is believed that the DMZ plays a role of cutting. The material separation point is located at the apex of the DMZ, and the bottom boundary of the DMZ is the slip-line. The most extensively used is Waldorf’s [20] model for calculating ploughing force. However, this approach ignores material flow inside the DMZ. The other model, proposed by Fang and Fang [21] or Jin and Altintas [22], is to designate the material separation point on the tool edge and establish a slip-line field along the tool edge. In order to analyze the stress distribution of the tool edge, Jin’s slip-line model is used to calculate the edge force.

### 3.2.1 Calculation of edge force

Figure 9a shows the slip-line field established by Jin and Altintas [22]. In the BQS region above the material separation point S, the direction of shear stress $\tau$ is from S to B, and the intersection angle between the $\alpha$ slip-line and the tangential direction of tool edge is $\eta$. In the SFA region below the material separation point S, the direction of shear stress $\tau$ is from S to A, and the intersection angle between the $\beta$ slip-line and the tangential direction of tool edge is $\eta$. $\sigma_n$ is the normal stress on the tool edge of BA. Point N is any point on the tool edge, point M is the intersection of $\beta$ slip-line where point N is located with BE, and $\theta$ is the angle between ON and OA.

As shown in Fig. 9b, according to the stress Mohr circle, the normal stress and shear stress of the tool edge are expressed in BS section and SA section as follows:

\[
\begin{align*}
\begin{cases}
\sigma_n = -p - k\sin(2\eta) \\
\tau = k\cos(2\eta)
\end{cases} \\
\text{along edge BS}
\end{align*}
\]

\[
\begin{align*}
\begin{cases}
\sigma_n = -p - k\sin(2\eta) \\
\tau = -k\cos(2\eta)
\end{cases} \\
\text{along edge SA}
\end{align*}
\]

where $p$ is hydrostatic pressure and $k$ is the maximum shear stress, and $\eta$ determined by the following equation:

\[
\eta = 0.5\cos^{-1}(m)
\]

where $m$ is the friction factor, which is equal to 0.99 [20].

The stress state of the whole slip-line field may be calculated simply by knowing the stress state of one point after the slip-line field has been defined. The hydrostatic pressure at point A is calculated as follows [20]:

\[
p_A = k(1 + 2(\frac{\pi}{4} - \rho + \eta))
\]

where $\rho$ is the prow angle, which is equal to 10° [23].

Assuming that the maximum shear stress along the slip line is constant, the relationship between the hydrostatic pressure and the maximum shear stress according to the Henkey’s equation is:

\[
p - 2k\omega = \text{cons} \tan \tau
\]

Therefore, the hydrostatic pressure at N point can be obtained by the following equation:

\[
p_N = p_M - 2k(\omega_1 - \omega_2)
\]

where $\omega_1$ and $\omega_2$ are the inclination angle of the $\beta$ slip-line with respect to the x-axis at point M and point N, expressed by the following equation:

\[
\begin{align*}
\omega_1 &= -\eta \\
\omega_2 &= \frac{\pi}{2} - \theta - \eta
\end{align*}
\]

by Eqs. (1–6), the component forces in the cutting and thrust directions along BS and SA can be calculated as follows:
where \( r_e \) is the cutting edge radius and \( w \) is the cutting width. For the uncoated tool, \( \theta_s \) is 39° [24]; for coated tools, \( \theta_s \) is 25° [21]. The average friction coefficient of the tool rake face is:

\[
\mu = \frac{Q_i}{Q_c} = \frac{F_t - F_{tp1} - F_{yp2}}{F_c - F_{cp1} - F_{cp2}}
\]

The maximum shear stress at the tool edge is assumed to be equal to the shear flow stress in the PDZ. The flow chart to calculate the maximum shear stress is shown in Fig. 10. The maximum shear stress obtained in this way is a pure shear stress, which is not affected by the edge radius. The shear angle is calculated using the shear flow stress and the measured cutting force.

\[
\Delta s_1 = \frac{t_u}{10 \cdot \sin \phi}
\]

3.2.2 Verification of the normal stress distribution at the tool edge

In order to verify the accuracy of ploughing force obtained by the slip-line model, a finite element model was established using DEFORM-2D/3D commercial software, and the normal stress distribution at the tool edge obtained by the slip-line model and the finite element model were compared. The selected cutting speed is 300 m/min and feed rate is 0.1 mm. The Coulomb law friction was adapted the tool-chip interface, and the friction coefficient was set to 0.9. The cutting force and thrust force obtained by simulation are 227.4 N and 150.7 N, respectively, which are close to the experimental results (243 N and 204 N). As shown in Fig. 11a, along the tool edge, point A is the starting point and point B is the end point, and 20 points are taken to record the normal stress data. The normal stress distribution under 10 asynchronous steps is aggregated and compared with the prediction of the slip-line model, as shown in Fig. 11b. It can be seen that the normal stress distribution of the slip-line model and the finite element model are generally consistent.

3.2.3 Average friction coefficient and apparent friction coefficient

Figure 12a shows the average friction coefficient \( \bar{\mu} \) and the apparent friction coefficient \( \mu_{app} \) after separating the edge force, and Fig. 12b shows the variation of \( \mu_{app} - \bar{\mu} \). It can be seen that when the feed rate is 0.2 mm (test 11–14) or 0.3 mm (test 15–18), the variation of \( \mu_{app} - \bar{\mu} \) increases as the cutting speed increases, which is consistent with Molinari’s [14] conclusion. At the feed rates of 0.05 mm (test 1–4) and 0.1 mm (test 5–10), the friction properties change significantly due to
changes in the adhesion morphology, so there is no such rule. As the cutting thickness increases, the fraction of edge force in total cutting force decreases and the variation of $\mu_{\text{app}} - \bar{\mu}$ displays a declining trend. The apparent friction coefficient under some cutting conditions is greater than 1, but after separating the edge force, the average friction coefficient is less than 1. Many researchers’ experiments have demonstrated that when the feed rate is small, the thrust force is larger than the cutting force owing to the edge force. This also implies that when the edge radius and the cutting thickness are in the same order of magnitude in cutting process, the force generated by the size effect of tool edge should not be overlooked, particularly in the study of friction on the tool rake face. It can be seen from the Fig. 12a that the average friction coefficient of uncoated tools is slightly larger than that of coated tools. The reasons for this will be discussed in Section 3.3.

### 3.2.4 Relationship between average friction coefficient and adhesion

As mentioned above, there are four adhesion types of coated and uncoated tools. For the adhesion morphology that the adhesion only occurs at a distance from the tool edge, it is defined as the initial sticking morphology; for the adhesion morphology that the adhering layer fully covers the tool rake face, it is defined as the stable sticking morphology; for the adhesion morphology that the fully covered adhering layer and the partially covered adhering layer coexist, it is defined as the stable sticking-sliding morphology; for the adhesion morphology of the adhering layer with only the partially covered adhering layer, it is defined as stable sliding morphology. It can be seen from Fig. 13 that at the low feed rates, the adhesion morphology undergoes change...
of initial sticking morphology, stable sticking morphology, stable sticking-sliding morphology, and stable sliding morphology as the cutting speed increases. At higher feed rates, there are almost no initial sticking morphology and stable sticking morphology. The adhesion morphology changes from stable sticking-sliding morphology to stable sliding morphology as the cutting speed increases.

Figure 14 shows the range of average friction coefficients under different adhesion morphologies. Among them, the average value of the average friction coefficient of the initial sticking morphology is 0.567, of the stable sticking morphology is 0.884, of the stable sticking-sliding morphology is 0.816, and of the stable sliding morphology is 0.703. In the initial sticking morphology, the temperature of the tool-chip interface is lower due to the lower cutting speed and feed rate, so only a small amount of adhesion occurs at a distance from the tool edge. The place where adhesion occurs may be located at the highest temperature of the tool-chip interface. Hong et al. [26] once achieved low-temperature cutting by infiltrating liquid nitrogen, and the result showed that the friction coefficient was greatly reduced during low-temperature cutting. This is because sticking is not easy to produce at low temperatures, and the sliding friction coefficient is much smaller than the sticking friction coefficient. So the initial sticking morphology dominated by sliding friction will have a smaller friction coefficient. When the cutting speed or feed rate increases, the temperature of the tool-chip interface rises, and the fluidity of adhesion increases, and the adhesion is more likely to be taken away by the chip. Therefore, the stable sliding morphology will be formed at the highest temperature at a distance from the tool edge. Since the temperature rises, the thermal softening effect increases, the shear stress decreases, and the friction coefficient decreases, which is called the self-lubricating effect by Child [2]. For steel, it occurs at speeds greater than tens of m/min [14]. Nevertheless, this work shows that the self-lubrication effect is also related to the feed rate, because the feed rate significantly affects the temperature of the tool-chip interface as well.

Courbon et al. [28] held the view that in the sticking zone, the tool-chip interface can be approximated as a complete contact due to higher contact pressure, and in the sliding zone, it transforms into incomplete contact due to the decrease in contact pressure. In addition, the concept of incomplete contact thermal resistance was introduced.

Fig. 13 Variation of adhesion morphology with cutting parameters: a coated tool, b uncoated tool

Fig. 14 Friction coefficient under different adhesion morphologies
Figure 15a and b shows the adhesion morphologies of complete contact and incomplete contact under the SEM. As noted previously, the temperature of the tool-chip interface rises and the fluidity of material increases as the cutting speed or feed rate increases. The adhesion is more easily taken away by the chip, resulting in the formation of partially covered adhering layer, known as the sliding zone. Consequently, the formation of the sliding zone is very likely to be dominated by temperature factors. As we know, the heat source of the tool-chip interface is the moving heat source [25], which results in a maximum temperature at a distance from the tool edge. This might also explain why the sliding zone is located far away from the tool edge. When the highest temperature reaches the critical point that can form adhesion, the sticking zone with a distance from the tool edge is generated. If the adhesion morphology depends on the normal stress distribution of the tool-chip interface, it is difficult to explain that the adhesion only occurs at a distance from the tool edge under low feed rate and low cutting speed, and it is also difficult to explain that the adhesion of the uncoated tool is at a distance from the tool edge at some cutting parameters. Of course, the adhesion morphology of the tool rake face may be a coupling effect of temperature and stress, and the dominating component should be quantified and investigated further. However, the contact area is reduced and the total shear stress decreases due to incomplete contact. Therefore, incomplete contact is another reason for the reduction of the friction coefficient in addition to the thermal softening effect. The temperature and stress of the chip contact surface will be discussed in the following section.

### 3.3 Temperature of tool-chip interface

In the metal cutting process, cutting temperature exerts a great influence on cutting force, tool-chip friction coefficient, and recrystallization. Therefore, the establishment of an accurate temperature analytical model is important to study the mechanism changes of the cutting process.

The temperature rise during the cutting process is mainly produced by three heat sources: the plastic deformation energy in the PDZ, the friction energy in the second deformation zone (SDZ), and the plastic deformation and the friction energy in the third deformation zone (TDZ). Since the cutting energy consumption of the TDZ occupies a relatively small amount in the macro-cutting, its influence was ignored. For the calculation of the temperature rise in the PDZ and the SDZ, the model proposed by Silin was used [29]. The calculation model is as follows:

The maximum temperature rise caused by plastic deformation in the PDZ is:

$$\Delta T_{\text{max}} = \frac{k}{c_w \rho_w \tan \phi} \cdot \text{erf} \left( \frac{R \tan \phi}{4} \right)$$

(12)

$$R = \frac{c_w \rho_w V_t u}{\lambda_w}$$

(13)

where $c_w$, $\rho_w$, and $\lambda_w$ are the specific heat capacity, the density and the thermal conductivity of the workpiece. $\text{erf}$ is the error function.

The average temperature rise is obtained by the following equation:

$$\Delta T_y = \begin{cases} 0.685 \rho \Delta T_{\text{max}} , & R \tan \phi \leq 5 \\ 0.620 \rho \Delta T_{\text{max}} , & 5 \leq R \tan \phi \leq 20 \\ 0.820 \rho \Delta T_{\text{max}} , & 20 \leq R \tan \phi \end{cases}$$

(14)

The friction heat source in the SDZ is expressed as follows:

$$q_f = \frac{Q_{\text{f}} V_c}{w l_c} = \tau_f V_c$$

(15)

where, $V_c$ is the chip flow speed, $l_c$ is the tool-chip contact length, and $\tau_f$ is the average friction stress.

The heat partition coefficient of the tool-chip interface is defined as the proportion of the total heat generated in the SDZ into the chip, can be estimated as follows:

![Fig. 15 Adhesion morphology: a complete contact ($V=50$ m/min, $f=0.2$ mm), b incomplete contact ($V=200$ m/min, $f=0.2$ mm)](image)
The maximum temperature rises of the tool-chip interface due to friction is:

$$\Delta T_{\text{f max}} = \frac{0.565R_0\eta_1l_1}{\lambda_w \sqrt{N_T}}$$  \hspace{1cm} (17)

$$N_T$$ is the thermal parameter, which is calculated by the following equation:

$$N_T = \frac{V_1l_1}{2\alpha_w}$$  \hspace{1cm} (18)

The average temperature rises of the tool-chip interface due to friction is:

$$\Delta T_f = \frac{0.377R_0\eta_1l_1}{\lambda_w \sqrt{N_T}}$$  \hspace{1cm} (19)

After calculating the average temperature rise and the maximum temperature rise of the PDZ and the SDZ, the average temperature and the maximum temperature of the PDZ and the SDZ can be obtained. The average temperature of the PDZ, the average temperature, and the maximum temperature of the SDZ are:

$$T_s = \Delta T_s + T_0$$  \hspace{1cm} (20)

$$T_f = \Delta T_s + \Delta T_f + T_0$$  \hspace{1cm} (21)

$$T_{\text{f max}} = \Delta T_{\text{f max}} + \Delta T_{\text{f max}} + T_0$$  \hspace{1cm} (22)

Due to its thermophysical properties that differ from cemented carbide, the tool coating has a significant impact on the temperature of the tool-chip interface. Grzesik et al. [30, 31] investigated the effect of several coatings on the temperature of the tool rake face and discovered that some coated tools such as TiC/TiN coating had greater cutting temperatures than uncoated tools, and others such as TiC/Al2O3/TiN coating, had lower temperatures. There are many factors that determine the temperature of the tool rake face, including the type, structure, and material of the coating. It is certain that the thermal conductivity of the tool coating is less than that of cemented carbide, which allows more heat to enter the chips, making the high temperature concentrated on the tool rake face and reducing the temperature of the tool substrate.

In actuality, whether the temperature of the tool-chip interface of the coated tool is lower than that of the uncoated tool depends on the combined effect of the temperature rise of the PDZ and the SDZ. Figure 16a indicates that the temperature rise of the coated and uncoated tools are similar in the PDZ, while the temperature rise of the coated tool in the SDZ is greater than the uncoated tool. The average and maximum temperatures of the tool-chip interface of the coated tool are greater than those of the uncoated tool, as shown in Fig. 16b. The higher the temperature, the stronger the self-lubricating effect. This means that the friction angle of the rake face of the coated tool is smaller, the shear angle is larger, and the angle between the combined force of the shear stress and normal stress on the shear plane and the cutting direction is smaller. Therefore, the tool-chip contact length is reduced, and the cutting force is smaller. This is consistent with the experimental results.

Figure 17a shows the average temperature of the tool-chip interface with different adhesion morphologies. It can be seen that from the initial sticking morphology to the stable sliding morphology, the average temperature of the tool-chip interface gradually increases. Figure 17b shows the relationship between the average temperature of the tool-chip interface and the average friction coefficient. It can be seen that the average temperature and the average friction coefficient have a relatively obvious non-linear relationship, and the average friction coefficient first increases and then decreases with the increase of the cutting temperature. Fitting with a cubic polynomial, the correlation coefficient $R^2$ is 0.84. This is very much in line with the previous proposal that temperature is the dominant factor in the formation of adhesion, and it shows that there is an inseparable relationship between temperature, adhesion and friction coefficient.

### 3.4 Normal stress of the tool-chip interface

The normal stress distribution on the tool rake face is shown in Eq. (23) [33]. Among them, $\sigma_0$ is the normal stress of the tool edge, and $x$ is the distance to the tool edge. $x = 0$ is at point B. $\xi$ is the normal stress distribution index. This normal stress distribution model has been verified and applied by most researchers. For the stress distribution index, Moufki et al. [33] pointed out that $\xi = 2$ is an appropriate value, and Zhou et al. [34] chose $\xi = 3$. Atkins’ [35] research showed that $\xi < 1$ when the sticking zone is dominant, and $\xi > 1$ when the sliding zone is dominant.

$$\sigma_x = \sigma_B \left( 1 - \frac{x}{l_c} \right)^\xi$$  \hspace{1cm} (23)
According to Eq. (23), the force $Q_c$ of the tool rake face can be expressed by the following equation:

$$Q_c = w \int_0^l \sigma_B \left( 1 - \frac{x}{l_c} \right) \, dx$$  \hspace{1cm} (24)

Equation (24) can be transformed into an equation about the normal stress distribution index:

$$\xi = \frac{\sigma_B w l_c}{Q_c} - 1$$  \hspace{1cm} (25)

Figure 18a shows the stress distribution index obtained from Eq. (25) when the feed rate is 0.1 mm. It can be seen that when the cutting speed is greater than 50 m/min, the normal stress distribution index is between 3 and 5. When the cutting speed is 20 m/min, the normal stress distribution index under the initial sticking morphology is larger. A larger stress distribution index means that the normal stress far away from the tool edge is smaller, as shown in Fig. 17b, but the adhesion is formed at a point far away from the tool edge, which shows that the normal stress has little effect on the adhesion.

Figure 19a shows the average normal stress of different adhesion morphologies. It can be seen that, except for the initial sticking morphology, the average normal stress is between 100 and 200 MPa, and the range of change is small. Figure 19b shows the relationship between the average normal stress and the average friction coefficient. It can be seen that the correlation between the two is poor. The cubic polynomial fitting is also used, and the correlation coefficient is only 0.56. It demonstrates that the normal stress of the tool-chip interface has little influence on the adhesion morphology and friction coefficient.
3.5 Cutting force and specific cutting energy

Cutting force and specific cutting energy are critical characterization approaches for studying the cutting mechanism, as well as important indications of cutting performance. Shear angle is a key factor that cannot be ignored when studying cutting force and specific cutting energy. The accuracy of the shear angle model is linked to the accuracy of the modeling of the cutting force and specific cutting energy. For the shear angle, Zvorykin gave the following equation to calculate [36]:

\[
\beta = A - \phi - \alpha
\]  

(26)

The \(\alpha\) is the tool rake angle. For 6061-T6 aluminum alloy, the value \(A\) is 35° [36]. Transform Eq. (26) into Eq. (27), then the value of \(A\) can be expressed in terms of friction angle \(\beta\) and shear angle \(\phi\). It should be noticed that \(\beta\) is not the real friction angle if taking into account the ploughing force, but should be expressed by Eq. (28):

\[
A = \phi + \frac{\beta - \alpha}{2}
\]  

(27)

\[
\beta = \frac{F_{c} - F_{cp2}}{F_{c} - F_{tp2}}
\]  

(28)

where the shear angle \(\phi\) and the ploughing force \(F_{cp2}\) and \(F_{tp2}\) are obtained from the flow chart in Fig. 10. Figure 20 shows the value of \(A\) under different cutting parameters. It can be seen that the value of \(A\) is consistent with the reference value under the cutting parameters with a higher feed rate. In the case of low feed rate, the value of \(A\) is lower than

![Graph](graph.png)

Fig. 18 a Normal stress distribution index (feed=0.1 mm), b normal stress distribution under different indexes

![Graph](graph1.png)

Fig. 19 a Adhesion morphology versus average normal stress, b average normal stress versus average friction coefficient
the reference value. This is due to the fact that the actual effective rake angle is smaller than the tool rake angle under the existence of the tool edge radius. In order to ignore the influence of the edge radius on cutting, the cutting force and specific cutting energy of 0.2 mm and 0.3 mm feed rate are analyzed in the following.

### 3.5.1 Cutting force

For the modeling of cutting force, Oxley’s parallel shear band model is the most widely used, and its shear angle model considering work hardening is also used by many researchers. As shown in Fig. 21a, in Oxley’s parallel shear band model, the shear stresses on the upper and lower boundaries of the shear band are different, and the torque generated needs to be offset. Therefore, it is considered that the normal stress acting on the chip on the shear surface is not uniformly distributed, and the normal stress near the edge point A is larger. In the cutting process, the shear band is very thin, so it can be considered that the shear stresses on the upper and lower boundaries of the shear band are not much different. Moreover, the length of the shear band is much larger than the width, so the normal stress for the chip at point A is almost the same as that at point E. According to slip-line theory, the shear stress on the shear zone is the Mises stress (maximum shear stress); hence, the shear zone’s center line AE can be employed as a slip line (arctangent line), as shown in Fig. 21b. Assuming that AE is a straight line, it can be considered that the hydrostatic pressures on AE are equal everywhere. Since the shear direction is the direction of the maximum shear stress, the hydrostatic pressure on AE is the normal stress acting on the chip; that is, the normal stress acting on the chip is equal everywhere. As shown in Fig. 21c, when the normal stress of the workpiece acting on the chips is uniformly distributed, the intersection point B of the resultant force R and the shear surface is located at the middle of AE and does not change with the change of the shear angle. Because the horizontal position of point B remains unchanged as the angle reduces and increases, the intersection point C of the resultant force R and the tool rake face will move down, as shown in Fig. 21d. The contact length of the chip is shortened if the normal stress distribution index does not change. This is why the chip contact length of the coated tool is shorter than that of the uncoated tool, and why the chip contact length lowers as the cutting speed increases. Because they contain a decrease in the friction coefficient.

According to the slip-line model, the expressions of the shear force \( F_s \) and pressure \( F_n \) on the tool rake face are as follows:

\[
\begin{align*}
F_s &= k \cdot \frac{w_t}{\sin\phi} \\
F_n &= k \cdot (1 + 2(\frac{t}{s} - \phi - \rho)) \cdot \frac{w_t}{\sin\phi}
\end{align*}
\]  \( \tag{29} \)

Then, the expression of the component force \( F_{cut} \) of the resultant force of the shear surface acting in the cutting direction is as follows:

\[
F_{cut} = F_c \cos\phi + F_n \sin\phi = F_c - F_{cp2}
\]  \( \tag{30} \)

The connection between the average friction coefficient and the shear flow stress is shown in Fig. 22a. It can be seen that when the average friction coefficient increases, the shear stress rises as well. This is because when the average friction coefficient increases, the shear angle decreases, the shear velocity decreases, the heat source intensity on the shear surface decreases, and the thermal softening effect decreases. The relationship between the shear stress and the average friction coefficient can be obtained, as in Eq. (31). The connection between the cutting force coefficient and the average friction coefficient is shown in Fig. 22b. It can be seen that the cutting force coefficient increases as the friction coefficient increases. Substituting Eq. (31) into Eq. (29), combined Eq. (30) and Eq. (32), and assuming that \( \mu \) is equal to \( \tan\beta \), the prediction model for the cutting force coefficient with the average friction coefficient can be obtained, as in Eq. (33). Figure 22b shows that the predicted curve can better fit the experimental results, which means that the slip-line model can describe the relationship between the friction coefficient and the cutting force coefficient.

\[
k = 110.2 + 140.7\mu
\]  \( \tag{31} \)

\[
K_c = \frac{F_{cut}}{w_Fu}
\]  \( \tag{32} \)

\[
K_c = (110.2 + 140.7\mu) \cdot \left( \cos \left( \frac{\pi}{4} - \frac{\arctan \mu}{2} \right) + \left( 1 + 2 \left( 1 - \frac{\arctan \mu}{2} - \rho \right) \right) \right)
\]  \( \tag{33} \)

---

**Fig. 20** The value of \( \alpha \) under different cutting parameters

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Specific cutting energy

Shaw [37] considered that cutting energy consumption is made up of four components: shear deformation energy on the shear surface, the friction energy on the tool rake face, the energy for new surface formation, and kinetic energy of the chip. Among them, the energy of new surface formation and the kinetic energy of the chip are neglected due to their smallness. During the cutting process, the specific cutting energy can be described as follows:

\[ W = \frac{F_c V}{VW t_u} = \frac{F_c}{w t_u} \tag{34} \]
The shear deformation energy on the shear surface can be calculated as follows:

\[ W_s = \frac{F_s V_s}{V_{mt} u} = \frac{k \cos \alpha}{\cos(\phi - a) \sin(\phi)} \]  

(35)

The friction energy on the tool rake face can be calculated as follows:

\[ W_f = \frac{Q_t V_c}{V_{wt} u} = \frac{Q_t \sin(\phi)}{w_{tc} \cos(\phi - a)} \]  

(36)

Chetan et al. [38] studied the specific cutting energy consumption under MQL. They argued that increasing cutting speed lowers specific cutting energy because the thermal softening effect lowers shear stress in the shear zone, lowering shear energy, and increasing feed rate lowers specific cutting energy due to lower friction energy. Since the shear angle determines the shear area and thus affects the shear energy, the shear angle is also a factor that cannot be ignored. The relationship between the specific shear energy and specific friction energy and the average friction coefficient is shown in Fig. 23a and b. It can be seen that both specific shear energy and specific friction energy increase with the increase of the average friction coefficient. The specific shear energy increases by 442.6 mJ/m³ and the specific friction energy increases by 100.8 mJ/mm³ when the average friction coefficient rises from 0.605 to 0.872. As a result, while increasing the friction coefficient directly increases the specific friction energy, it also decreases the shear angle and raises the specific shear energy, which is the major cause of the overall specific cutting energy increase.

The relationship between the shear angle and the specific shear energy is depicted in Fig. 24. As can be observed that the specific shear energy decreases as the shear angle increases, which demonstrates that the shear angle is a significant factor in the change of the specific shear energy. In reality, the material properties influence the change in particular shear energy. For 6061-T6 aluminum alloy, when the feed rate is 0.1 mm, the cutting speed is increased from 20 to 100 m/min, the PDZ temperature is increased from 70 to 128 °C, and the shear stress is reduced from 254.7 to 240.4 MPa, with a decline of only 5.6%. When Chetan et al. [38] machined Nimonic 90 with a feed rate of 0.12 mm, which increased the cutting speed from 20 m/min to 100 m/min, the PDZ temperature increased from 413.64 to 556.82 °C, and lowered the shear stress from 1602.5 to 1246.7 MPa, with a decline of only 22%. So the Nimonic 90 is more sensitive to thermal softening effects.

### 3.6 Discussion

The temperature of the tool-chip interface affects the adhesion of the tool rake face and the friction coefficient, and the friction coefficient affects the cutting force specific cutting energy, which in turn affects the temperature of the tool-chip interface. This is mutual coupling and balance. This equilibrium will be broken when the cutting parameters or the condition of the tool-chip interface are changed, and a new balance will be reached. The appropriate range of processing parameters can be examined by investigating the adhesion evolution of different materials under different cutting conditions in order to reduce cutting energy consumption and improve production efficiency.

With the increase of cutting speed and feed rate, due to the gradual increase in the temperature of the tool-chip interface, different adhesion morphologies are produced. An inference about adhesion formation can be made:

From the moment the tool touches the workpiece to the development of stable cutting, the temperature gradually rises and finally reaches a stable state. As the cutting progresses, the temperature of the tool-chip interface rises, and the adhesion formation goes through the following steps:
At first, due to the low temperature, there is no adhesion. After the maximum temperature reaches that the adhesion can be formed, the adhesion is first generated at a distance from the tool edge, developing the initial sticking morphology. The initial sticking changes the geometry of the tool rake face, and the adhesion is more likely to accumulate. Then, when the temperature of each point on the tool-chip interface reaches that the adhesion can be formed, a stable sticking morphology is formed. After that, the temperature continued to rise. And at the maximum temperature point, the adhesion is no longer strong due to the thermal softening effect, and part of the bonding material is taken away by the chips, forming the sliding zone with incomplete contact. At this time, a stable sticking-sliding morphology is formed. Finally, the temperature of each point of the tool-chip interface reaches that the sliding zone is formed, and the stable sliding morphology is formed. To explain the production of the adhering layer on the tool rake face, the adhesion rate method considering temperature distribution may be used to estimate the adhering layer formation process during machining. In the previous research, although there have been many temperature and stress distribution models of the tool-chip interface, there are few researches relating them to the adhesion morphology. This may need to be studied in the future.

4 Conclusions

In order to explore the relationship between the tool adhesion morphology and the cutting force under different cutting parameters, the orthogonal cutting experiments were carried out at the feed rates of 0.05–0.3 mm and the cutting speeds of 20–300 m/min, and the coated tools and uncoated tools were compared. Some conclusions are summarized as follows:

In the process of increasing cutting speed and feed rate, there are four different adhesion morphologies. The dual zone theory proposed by Zorev only exists in a small range of cutting parameters. And the formation of tool surface adhesion is mainly related to temperature. The sticking zone may only occur far away from the tool edge, and the sliding zone may occur near the tool edge.

A method for separating the edge force is suggested and verified by using FEM. The apparent friction coefficient and average friction coefficient are analyzed. The results reveal that the influence of the tool edge on the force may be ignored only when the tool edge radius is one order of magnitude smaller than the uncut chip thickness.

When the cutting speed increases, the temperature of the tool-chip interface increases, and the fluidity of the material increases, making the stable sliding morphology easier to generate. The decrease in friction coefficient with cutting speed is not only due to the self-lubricating effect produced by thermal softening, but also due to the incomplete contact of the sliding morphology. When the cutting speed reaches 300 m/min, the adhesion morphology is a stable slip morphology, and the friction coefficient is small. Therefore, it is recommended that the cutting speed of 6001-T6 aluminum alloy is greater than 300 m/min.

Shear angle is the major factor influencing the cutting force and the specific cutting energy. Different adhesion morphologies have different friction coefficient ranges, and the friction angle affects the change of shear angle, thereby causing changes in the cutting force and the specific cutting energy. The influence of force on the temperature, in turn, affects the formation of adhesion. Therefore, the adhesion morphology and cutting force are coupled with each other through friction coefficient and temperature.

Author contribution Jianchao Yu contributed to the conception of the study; Yuxing Ding performed the experiment, contributed significantly to analysis and manuscript preparation, performed the data analyses, and wrote the manuscript; Zhikai Lu assisted the experiment.

Funding This work is supported by Natural Science Foundation of Fujian Province (Grant No. 2019J01212), National Natural Science Foundation of China (Grant No. 51975123), Science and Technology Plan Project of Fuzhou (Grant No. 2018-G-57), Fuzhou University Talent Fund (Grant No. XRC-18003).

Declarations

Ethics approval The manuscript has not been submitted or published anywhere. It will not be submitted elsewhere as well.

Consent to participate All authors consent to participate in the author team of this submitted manuscript.

Consent to publish The manuscript was approved by all authors to publish.

Competing interests The authors declare no competing interests.
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