Research on breakage characteristics in side milling of titanium alloy with cemented carbide end mill

Yanjie Du 1 · Caixu Yue 1 · Xiaochen Li 1 · Xianli Liu 1 · Steven Y. Liang 2

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Abstract
Titanium alloy Ti6Al4V has the advantages of high specific strength, good heat resistance, and strong corrosion resistance, which is widely used in the manufacturing of aerospace industrial parts. However, in the side milling of titanium alloy, the temperature of the cutting area is high, and the cutting edge position is prone to breakage, which affects the surface quality of the workpiece. In order to reveal the tool damage failure mechanism in the milling process of titanium alloy, firstly, the impact force model when the tool cuts into the workpiece from the empty stroke and the milling force model during milling are established to obtain the cyclic load characteristics and impact effect of the tool. Then, based on the fatigue crack propagation theory and the energy balance equation of sliding crack, the elastic modulus and crack propagation law of tool material under different cutting impacts are analyzed. The interval method is used to recalculate the initial and critical damage value of tool material fracture in the maximum range. Finally, the limit conditions of the edge impact fracture of the end mill are established, and the safe cutting area of tool breakage is redefined. Through the milling test of titanium alloy, the impact damage morphology of tool in different states is redefined. The obtained redefined tool safety area provides a theoretical basis for high-speed and high-efficiency milling of titanium alloy, tool breakage, and tool life.

Keywords Side milling · Tool breakage · Cutting impact · Interval method

1 Introduction
At present, material failure is mainly to calculate damage by introducing microscopic defects. Starting from the constitutive relationship of material, material failure process undergoing damage evolution from the initial state to complete damage is studied [1]. High-speed milling of titanium alloys requires cemented carbide tool to have good performance in hardness and toughness, the cemented carbide tool with finer grain can be selected to increase the impact fracture performance with the increase in hardness, and the toughness will decrease. The cyclic loading, no-loading cooling, and cut-in impact on the tool in the milling process will lead to tool fatigue damage, chipping, and sticking [2]. Based on experiments, Girolamo et al. [3] concentrated stress on micro-defects, applied cyclic loading, and analyzed experiments to modify the theory of fracture strength on the basis of microcracks.

The original microcrack in cemented carbide tool is the stress concentration area in the tool fatigue damage, and it will develop into the source of crack propagation under high stress concentration, which seriously affects the tool fatigue damage resistance. Under different cutting conditions, the tool damage will also have different changes. Firstly, there is no related experiment to prove fatigue loading of cutting tool in the milling process, and the tool breakage properties change with tool material and workpiece material, so there is not specific damage degree and damage failure criteria [4]. However, it has been explored that the crack growth rate of tool material under cyclic loading can be studied by Paris theory, which can further describe fatigue failure. Based on analyzing the experimental results, Gee et al. [5] designed fatigue experiments of cemented carbide material, and it is concluded that the fatigue
life of cemented carbide is not affected by loading frequency. Llanes et al. [6] selected experimental indexes including fracture toughness, bending strength, and crack growth rate to research 5 kinds of cemented carbide. The results showed that the thickness of binder Co layer in YG cemented carbide had a great influence on fracture toughness. Cheng et al. [7] established the fatigue damage model of cemented carbide tool, determined the fatigue damage limit of cemented carbide material under cyclic loading, and found that impact fracture is the final failure form of tool fatigue damage. Uhlmann et al. [8] analyzed the influence of residual stress and coating quality on the bonding properties of the coating and cemented carbide substrates. Torres et al. [9] studied crack propagation of WC-Co cemented carbide with cyclic bending fatigue experiment, which described the relationship between crack propagation rate and minimum tensile stress. Bhatia et al. [10] concluded that the fatigue life is determined by the crack propagation rate in fine-grained WC-Co cemented carbide according to the fatigue experiment.

In conclusion, the fatigue of cemented carbide material is the process that tool material continuously damages and eventually leads to fracture. In the process of milling, the impact effect of milling caused by intermittent cutting, the cyclic loading, and the vibration of the end mills will lead to a large increase in the damage and accelerate the failure process. Therefore, it is urgent to study the properties of cemented carbide material and simulate the actual cyclic loading and material damage of cemented carbide tools in high-speed milling of titanium alloy, so as to provide theoretical support for the failure mechanism of cemented carbide tool and impact fracture limit conditions.

Due to poor heat resistance, titanium alloy belongs to the typical difficult-to-machine material, and the milling of titanium alloy needs more complex manufacturing process. In the milling of titanium alloy, high-speed cutting causes serious tool breakage failure. So the end mill failure’s study has important significance during milling of titanium alloy.

Cemented carbide tools in the milling process will produce a lot of heat, and have good hardness and toughness in high-temperature conditions, which can be widely used as a tool material in the high-speed milling process. Even so, damage failure of cemented carbide tools in the high-speed milling also brought serious consequences [11–13]. Based on the experiment of M11 hardened steel, Gong et al. [14] analyzed the mechanism of tool damage by observing and summarizing the forms of tool damage and surface morphology. Sun et al. [15] established a tool damage detection model in cutting process by collecting the torque changes when the tool is damaged, and verified by experiments that the model has an accuracy rate of 91.18%. Ni et al. [16] used finite element simulation to analyze the change of stress on cutting tool with time. Based on the fatigue crack model, the initiation and propagation life of the cracks at different locations are studied, and it is inferred that the fatigue fracture is caused by the coalescence of cracks. Liu et al. [17] found that the cutting speed and feed speed were closely related to the fracture mode and failure mechanism of the tool in intermittent turning of hardened steel. Ding et al. [18, 19] used WC-6%Co and WC-20%Co cemented carbide tools for experiments. The experimental results were further analyzed by scanning electron microscopy, and the crack propagation was successfully observed, which can conclude that the crack propagation eventually led to the tool impact damage. Yan et al. [20] proved that the tool breakage failure also accounted for a large proportion in the actual milling process, and the tensile stress exceeding the tool tensile strength was the fundamental factor that led to the tool damage. Gong et al. [21] studied the fracture mechanism of cemented carbide tool by milling hardened steel, which further proved that the hardness of the workpiece had an effect on the tool failure, and the geometric model of tool wear and the fracture was established. Jiang et al. [22] used numerical method to reveal the mechanism of micro breakage of cemented carbide tool, and proved that micro breakage was mainly caused by mechanical impact, and had a tendency to continuously expand into the material. Based on the heavy duty intermittent cutting experiment of cemented carbide tool, Liu et al. [23] expounded the formation mechanism of tool breakage, analyzed the evolution of tool failure, and obtained the tool failure condition. Based on the stress-strength theory, Wang et al. [24] studied the reliability analysis of tool life, and the change of tool failure with time was described.

From the analysis of the domestic and foreign scholars study, tool damage characteristics of the research are mainly concentrated in the following aspects: tool breakage is based on the tool crack existing inside, and micro factors will extend with the milling; the impact caused by intermittent cutting and cyclic stress in the process of cutting will have an effect on tool failure, and the optimization of improving the cutting performance including the tool structure, and cutting parameters.

At present, the research on tool breakage is mostly qualitative analysis, which is difficult to calculate quantitatively. Therefore, this paper defines three types of slight chipping, chipping, and breakage of the tool by defining the shape of the tool after damage under different conditions. Based on the fatigue crack propagation theory and the energy balance equation of sliding crack, the elastic modulus and crack propagation law of tool material under different cutting impacts are analyzed. The interval method is used to recalculate the initial and critical damage value of tool material fracture in the maximum range. The limit conditions of the edge impact fracture of the
end mill are established, and the range of the tool safety area is obtained, which provides the basis for the optimization of the milling process parameters of titanium alloy.

2 Milling force modeling

2.1 Overall milling force modeling

In essence, cutting is a process to remove excess material. The shear deformation of the material is the result of complex thermodynamic coupling between tool and workpiece. For the tool, it is necessary to overcome the friction resistance of the chip and the workpiece, and also to overcome the shear deformation force, in order to maintain the cutting process [25]. When the cutting edge is absolutely sharp, the forces acting on the chip in the first shear zone are the normal pressure \( F_{ns} \) and the shear force \( F_s \), respectively. The force in the first shear zone is shown in Eqs. (1) and (2):

\[
F_s = \tau_0 A_e \frac{\tau_0 h A_e}{\sin(\phi_n) \cos \lambda_n} \quad (1)
\]

\[
F_m = F_t \tan(\phi_n + \beta_n - \alpha_n) \cos \lambda_n \quad (2)
\]

where \( h \) is the thickness of the undeformed chip, \( A_e \) is the cutting width, \( \beta_n \) is the normal friction angle, and \( \tau_0 \) is the shear stress at the shear exit.

According to the bond-slip model, the frictional force and normal stress on the rake face can be solved. The frictional force \( F_f \) and the positive pressure \( F_n \) in tool-chip interface can be expressed as:

\[
\begin{align*}
F_f &= \int_0^{L_t} \tau_0 \sin(\phi_n + \beta_n - \alpha_n) \cos \lambda_n \left( 0.07 \frac{\mu \sigma_n(x)}{H_v} \right) dx \\
F_n &= \int_0^{L_t} \sigma_n(x) dx
\end{align*}
\quad (3)
\]

where \( H_v \) is the Rockwell hardness of the material, \( L_t \) is the bonding zone length of the rake face, \( L \) is the total tool-chip contact length of the rake face, \( \mu \) is the friction coefficient, and \( \sigma_n \) is the normal stress.

\[
\mu = 1.061 e^{-0.014 V_v} \left( 1 - \left( \frac{T}{T_m} \right)^{3.6} \right) \quad (4)
\]

where \( V_v \) is the chip velocity, \( T \) is the average temperature on the rake face, and \( T_m \) is the melting point temperature of the workpiece material.

When the flank wear volume reaches a certain value, the width of the elastic contact zone remains unchanged, while the width of the plastic flow zone increases with the increase of the flank wear volume.

The friction force and extrusion force per unit length on the cutting edge of the end mill can be obtained by simplification, as shown in Eqs. (5) and (6) [26]:

\[
\begin{align*}
F_{rm}(VB) &= \frac{\sigma_1}{3} \cdot VB \\
F_{nm}(VB) &= \frac{\sigma_1}{3} \cdot VB
\end{align*}
\quad (5)
\]

\[
\begin{align*}
F_{rm}(VB) &= \tau_1 \cdot \left( VB - \frac{2}{3} VB^* \right) \\
F_{nm}(VB) &= \sigma_1 \cdot \left( VB - \frac{2}{3} VB^* \right)
\end{align*}
\quad (6)
\]

where \( VB \) is the flank wear volume, \( VB^* \) is the constant width of elastic contact zone, \( \tau_1 \) is the maximum shear stress of flank face, and \( \sigma_1 \) is the maximum normal stress of flank face.

In the oblique cutting considering the flank wear, \( \alpha_n \) is the normal rake angle of the tool, \( \eta_n \) is the chip outflow angle, \( \lambda_n \) is the inclination angle, \( F_t \) is the friction force on the flank face, \( F_n \) is the pressure on the rake face, \( F_{rw} \) is the tangential force on the flank face, and \( F_{tw} \) is the radial force on the flank face, as shown in Fig. 1 [27].

In the tool coordinate system, the tangential force \( dF_t \), the radial force \( dF_r \), and the axial force \( dF_a \) can be expressed as:

\[
\begin{align*}
dF_t &= (F_f \cos \eta_n \cos \alpha + F_s \cos \eta_n \cos(90 - \alpha) + F_{rw} - F_r \cos \eta_n \cos(90 - \phi_n) - F_{mn} \cos \eta_n \cos \phi_n)dz \\
dF_f &= (F_f \cos \eta_n \cos(90 - \alpha) - F_r \cos \eta_n \cos(90 - \phi_n) + F_{mn} \cos \eta_n \sin \phi_n)dz \\
dF_a &= (-F_r \cos(90 - \eta_n) + F_s \cos(90 - \eta_n) - F_{mn} \cos(90 - \eta_n))dz
\end{align*}
\quad (7)
\]

According to the geometric relationship in Fig. 2, the correlation between undeformed chip thickness \( h \), instantaneous contact angle \( \theta \), feed per tooth \( f_z \), and milling width \( A_e \) can be obtained [28]. The undeformed chip thickness \( h \) can be expressed as:
can be obtained according to the established model. According to the cutting parameters, tool geometric parameters, and the hardness of workpiece material, the friction characteristics in the milling of titanium alloys by cemented carbide end mill can be described, and the predicted milling force can be obtained according to the established model.

\[ h(\theta, A_c, f_z) = \begin{cases} R - \frac{R - A_c}{\sin \theta_1} \sin \theta_1 \\ \frac{R \cos \theta_2}{(f_z)^2 - \sec^2 \theta_2 \theta_2} - f_z \theta_2 \in (\theta, \angle XOD) \end{cases} \]

The end mill is discretized into \( m \) disks along the axial cutting depth, and the axial height of each disk \( dz \) can be expressed by \( dl = \tan \beta dz \). According to the geometric relationship, the \( dz \) can be represented by the product of the corresponding center angle \( d\theta \) and the tool radius \( R \) (Fig. 3).

\[ dz = \frac{R}{\tan \beta} d\theta \]

The cutting force exerted by the disk of finite thickness is calculated in the tool coordinate system, and the force exerted by the disk in the workpiece coordinate system is obtained through the coordinate system [29]. The transformation equation is as follows:

\[ \begin{bmatrix} dF_x \\ dF_y \\ dF_z \end{bmatrix} = \begin{bmatrix} -\cos \theta & -\sin \theta & 0 \\ \sin \theta & \cos \theta & 0 \\ 0 & 0 & -1 \end{bmatrix} \begin{bmatrix} dF_x \\ dF_y \\ dF_z \end{bmatrix} \]

In summary, in the milling process, the cutting forces in \( X \), \( Y \), and \( Z \) directions of the tool in the workpiece coordinate system are shown in Eq. (12):

\[ \begin{align*}
F_x &= \int_{\theta_n}^{\theta_0} \left( -F_c \cos \eta_1 \cos (90 - \eta_1) + F_c \cos \eta_1 \cos \alpha_1 - F_r + F_s \cos \eta_1 \sin (90 - \phi_n) + F_n \cos \eta_1 \sin \phi_n \right) \cos \theta - R \tan \lambda s d\theta \\
F_y &= \int_{\theta_n}^{\theta_0} \left( -F_c \cos \eta_1 \cos (90 - \eta_1) + F_c \cos \eta_1 \cos \alpha_1 - F_r + F_s \cos \eta_1 \sin (90 - \phi_n) + F_n \cos \eta_1 \sin \phi_n \right) \sin \theta - R \tan \lambda s d\theta \\
F_z &= \int_{\theta_n}^{\theta_0} \left( -F_c \cos (90 - \eta_1) + F_c \cos (90 - \eta_1) - F_s \cos (90 - \eta_1) - F_n \cos (90 - \eta_1) \right) R \tan \lambda s d\theta
\end{align*} \]

Equation (12) is a three-dimensional cutting force prediction model based on the friction model with variable friction coefficient. The variables involved in the model are analyzed. According to the cutting parameters, tool geometric parameters, and the hardness of workpiece material, the friction characteristics in the milling of titanium alloys by cemented carbide end mill can be described, and the predicted milling force can be obtained according to the established model.

2.2 Impact force modeling

2.2.1 Normal contact force modeling

The equivalent spring damping method is usually used to transform the contact collision problem into a continuous dynamic problem, so the contact force in the machining process is calculated according to the spring damping model [30]. The equivalent spring damping model is shown in Eq. (13). The specific expression of the normal contact force is as follows.

![Fig. 1 The forces in the oblique cutting area](image-url)
\[ F_{n1} = K_a \delta^e + d \dot{\delta} \]  \hspace{1cm} (13)

where \( K_a \) is the stiffness coefficient, \( \delta \) is the deformation of the collision object, \( e \) is the index of penetration depth, \( d \) is the damping coefficient, and \( \dot{\delta} \) is the relative velocity of the two objects.

According to Hertz elastic contact theory, when there is no particularly prominent bulge in the shape of the contact object and the contact is made in a standard geometrical shape, the contact stiffness coefficient and stiffness index can be obtained from the inherent property parameters of the material, and then the impact force model can be obtained.

\[ K_a = \sqrt{\frac{16R_3E_a^2}{9}} \]  \hspace{1cm} (14)

The specific expressions of \( R_3 \) and \( E_a \) in the above formula are as follows:

\[ R_3 = \left[ \frac{1}{R_1^2} + \frac{1}{R_2^2} \right]^{-1} \]  \hspace{1cm} (15)

\[ E_a = \left[ \frac{1-u_1^2}{E_{a1}} + \frac{1-u_2^2}{E_{a2}} \right]^{-1} \]  \hspace{1cm} (16)

where \( R_1 \) and \( R_2 \) are respectively the effective collision radii of two collision objects at the collision point, \( u_1 \) and \( u_2 \) are respectively Poisson’s ratio of the matrix material of two collision objects, and \( E_{a1} \) and \( E_{a2} \) are respectively the three-dimensional elastic modulus of the collective material of two collision objects.

The contact force model in the collision process can be obtained from Eq. (13), and solving the contact collision problem is the primary condition for using the equivalent spring damping method. For solving the damping coefficient, Eq. (17) adopts the hysteretic damping model, which can be specifically expressed as:

\[ D = \mu \delta^e \]  \hspace{1cm} (17)

where \( \mu \) is the hysteretic damping factor.
The hysteretic damping factor \( u \) can be expressed as:

\[
u = \frac{3K(1-\varepsilon^2)}{4\delta}
\]  

(18)

where \( \varepsilon \) is the ratio of normal relative velocity of two contact objects before and after collision.

### 2.2.2 Tangential contact force model

When two contact objects collide, the impact force of the tool which instantaneously invades the workpiece will produce normal impact force, and the relative motion between the tool and the workpiece will produce tangential friction force. Friction force is proportional to normal loading, and its direction is opposite to the relative sliding direction, and its value is not affected by the size of the contact area. The specific expression of the tangential contact force is:

\[
F_{s1} = \mu F_{n1}
\]  

(19)

The contact force model obtained in the local tool coordinate system is transformed into the cutting impact force model obtained in the workpiece coordinate system \((XY)\), which is calculated as follows [31]:

\[
\begin{align*}
F_{im,x} &= F_{n1} \sin \varphi_f + F_{s1} \cos \varphi_f \\
F_{im,y} &= F_{n1} \cos \varphi_f + F_{s1} \sin \varphi_f
\end{align*}
\]  

(20)

where \( F_{im,x} \) is the impact force in \( X \) direction, \( F_{im,y} \) is the impact force in \( Y \) direction, \( \varphi_f \) is the contact angle during the collision, and its calculation is as follows:

\[
\varphi_f = \pi - \arccos \left( 1 - \frac{Ae}{R} \right)
\]  

(21)

### 3 Damage of cemented carbide tool

The tool fatigue damage is affected by a variety of factors, which are complicated in the milling process. The tool damage can be regarded as a random situation. The milling with higher speed will produce great impact; assuming that the impact force acted on the tool is sufficient, the defects in the tool have a higher probability of breakage with more impact [32]. Since the tool damage life is related to the inherent properties of tool material, the tool fatigue damage life can be described by the physical properties of the tool.

In the process of impact fracture between cemented carbide tool and workpiece, it could be assumed that the impact phenomenon occurred under the large cutting force in the cutting process, which made cemented carbide tool fracture along the direction of the edge. In this paper, based on the experimental phenomena, tool breakage degree is divided into slightly chipping, chipping, and breakage, and the flank breakage width is measured. For the first time to participate in milling, the sharp tool nose will break when it contacts with the workpiece at the moment. So as to avoid causing the confusion for the different situation, in this paper, the tool breakage below 50\( \mu m \) is regarded as slightly chipping, which did not affect the milling process. The condition of tool breakage becomes increasingly serious with the milling progress. The tool breakage below 100\( \mu m \) is regarded as chipping, and the cutting state is close to the normal tool wear state. With the increase of tool breakage, the cutting force increases, the surface quality becomes worse, and obvious cracks appear on the workpiece surface. When the tool breakage along the flank face reaches 200\( \mu m \), the cutting condition is significantly serious, there is a large noise during the cutting process, and the burr phenomenon is obvious. At this time, the cutting temperature rises significantly, and the chip appears red around the cutting edge, resulting in workpiece surface quality being reduced, which is defined as tool breakage, as shown in Fig. 4.

### 3.1 Tool material damage model

Before analyzing the damage mechanism of cemented carbide tool, the initial and critical damage values of tool material must be determined, which represents the damage state of the tool in the initial state and the limit state. According to the existing researches, it can be known that the initial and the critical damage values are the inherent properties of tool material; in other words, these two values are independent of all external conditions. In order to determine the initial and critical damage values of tool material, a damage model is established for tool material. In the state of triaxial stress caused by external stress, the following assumptions are made: tool material damage is isotropic, there is initial cracks in tool material, and the crack propagation and joint eventually led to the tool damage failure.

Damage equivalent stress could be used to simplify the complex triaxial stress state into uniaxial stress state [33]. By equivalent transformation, the damage equivalent stress can produce the same elastic deformation energy density as the original triaxial stress state. It is assumed that the original triaxial stress state and the converted uniaxial stress state has the same effect on the damage evolution of tool material. Taking into account the quasi-unilateral condition of crack closure, the damage equivalent stress can be expressed as:
where $\sigma^*$ is the damage equivalent stress, $\sigma$ is the triaxial stress, $\nu$ is the Poisson’s ratio of tool material, $D$ is the damage value of tool material, and the value of $h_1$ is 0.2.

Considering the computability of the model and the background of engineering application, the triaxial stress $\sigma$ is related to the established cutting forces in this paper, where $F_x$, $F_y$, and $F_z$ represented the average value of cutting forces in three directions $X$, $Y$, and $Z$; the forces along $X$ and $Y$ direction contained impact force.

$$\sigma^* = \left\{ (1 + \nu)\langle \sigma \rangle^+ : \langle \sigma \rangle^+ - \nu(\text{tr} \sigma)^2 + \frac{1-D}{1-h_1 D} \left[ (1 + \nu)\langle \sigma \rangle^- : \langle \sigma \rangle^- - \nu(-\text{tr} \sigma)^2 \right] \right\}^{1/2}$$

Substitute Eqs. (18) and (19) into Eq. (20), and the forces act on tool material can be expressed as:

$$\sigma_c = \left\{ (1 + \nu)\langle \sigma \rangle^+ : \langle \sigma \rangle^+ - \nu(\text{tr} \sigma)^2 + \frac{1-D}{1-h_1 D} \left[ (1 + \nu)\langle \sigma \rangle^- : \langle \sigma \rangle^- - \nu(-\text{tr} \sigma)^2 \right] \right\}^{1/2} \left( \frac{1-D}{1-h_1 D} \right)^{-1/2}$$

After the equivalent transformation of the stress state, the triaxial stress state could be transformed into the uniaxial compressive stress $\sigma_c$, and the stress state of the tool could be represented. The material element contains sliding microcracks originally. According to the simplified condition that the microcracks are uniformly distributed and interact each other, tool material damage could be solved in the next step.

In order to study the crack propagation of brittle material under compressive stress, many micro mechanical models have been established. The sliding crack model has been widely used by Horri et al. [34], and the sliding crack morphology is proposed to study the crack behavior under compressive stress. The open crack appears at the tip of the microcracks, and the curved microcrack is replaced by a straight line. The experimental results have shown that at the initial stage of crack propagation, the direction of crack propagation has a great angle with the axial compressive stress. Therefore, the sliding crack model is simplified and modified in Fig. 5. The crack expanded along the direction of the maximum axial compressive stress, and the length of initial microcrack is $2c$, the length of each open crack is $l$, and the opening crack and the initial microcrack form an angle of $\theta$, as shown in Fig. 5 [33].

According to existing researches, the interaction between cracks is taken into account, and a sliding crack model containing a sliding microcrack array under uniaxial compressive stress could be established. Strains $\varepsilon_1$ and $\varepsilon_2$ were caused by the uniaxial compressive stress $\sigma_c$ [33],

$$
\begin{pmatrix}
\varepsilon_1 \\
\varepsilon_2
\end{pmatrix} = \begin{pmatrix}
\varepsilon_1^e + \Delta \varepsilon_1 \\
\varepsilon_2^e + \Delta \varepsilon_2
\end{pmatrix}
$$  \tag{26}

where $\varepsilon_1^e$ and $\varepsilon_2^e$ are the elastic strain of the non-damaged tool material, and $\Delta \varepsilon_1$ and $\Delta \varepsilon_2$ are the total damage strain caused by the sliding of initial microcrack and the propagation of crack sliding.

The elastic strain of tool material without damage could be defined as:

$$
\begin{pmatrix}
\varepsilon_1^e \\
\varepsilon_2^e
\end{pmatrix} = \frac{(k+1)(v+1)}{4E} \begin{pmatrix}
1 \\
\frac{k-3}{k+1}
\end{pmatrix} \begin{pmatrix}
\sigma_c \\
0
\end{pmatrix}
$$  \tag{27}

where $E$ is the elastic modulus of tool material, $k$ is the parameter determined by Poisson’s ratio, $k=5v$ for plane strain condition, and $k=[(3-v)/(1+v)]$ for plane stress condition.

According to study of Ravichandran et al. [35], based on the consideration of the current linear problem, it is assumed that the damage strain is linearly related to the uniaxial compressive stress $\sigma_c$, and the number of initial microcracks in tool material element is $N$, then the damage strain can be expressed as:

$$
\begin{pmatrix}
\Delta \varepsilon_1 \\
\Delta \varepsilon_2
\end{pmatrix} = N \begin{bmatrix}
S_{11} & S_{12} \\
S_{21} & S_{22}
\end{bmatrix} \begin{pmatrix}
\sigma_1 \\
\sigma_2
\end{pmatrix}
$$

$$
= N \begin{bmatrix}
S_{11} & S_{12} \\
S_{21} & S_{22}
\end{bmatrix} \begin{pmatrix}
\sigma_c \\
0
\end{pmatrix}
$$  \tag{28}

where $S_{11}, S_{12}, S_{21},$ and $S_{22}$ are constants, and $S_{12}=S_{21}$, and $\sigma_1$ and $\sigma_2$ are the normal stress in two directions in the plane.

If the interaction between microcracks is not taken into account, the above variables could be expressed by substituting them into the total damage strain. The total strain of the selected tool material element could be expressed as:

$$
\begin{pmatrix}
\varepsilon_1 \\
\varepsilon_2
\end{pmatrix} = \begin{pmatrix}
\varepsilon_1^e + \Delta \varepsilon_1 \\
\varepsilon_2^e + \Delta \varepsilon_2
\end{pmatrix}
$$

$$
= \frac{(k+1)(v+1)}{4E} \begin{pmatrix}
1 \\
\frac{k-3}{k+1}
\end{pmatrix} \begin{pmatrix}
\sigma_c \\
0
\end{pmatrix}
$$

$$
+ N \begin{bmatrix}
S_{11} & S_{12} \\
S_{21} & S_{22}
\end{bmatrix} \begin{pmatrix}
\sigma_c \\
0
\end{pmatrix}
$$  \tag{29}

Fig. 5 Slip propagation model of single crack

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Let $\varepsilon_1=\varepsilon$, and the above equation can be simplified as follows:

$$\sigma_c = \frac{E}{1+E NS_{11}} \varepsilon$$  \hspace{1cm} (30)$$

Under the condition of external loading, the strain of the damaged material is controlled by the elastic modulus $E$. The elastic modulus is the inherent parameter of tool material. With the milling process, the damage of tool material is continuously accumulated, and the elastic modulus is also reduced. Therefore, the elastic modulus of tool material degradation is used to define the damage of the material element [33]:

$$\sigma_c = E_D \varepsilon = E(1-D) \varepsilon$$  \hspace{1cm} (31)$$

$$D = 1 - \frac{E}{1+ENS_{11}}$$  \hspace{1cm} (32)$$

The damage value of tool material and elasticity modulus with degradation could be respectively solved to get the elastic strain energy $W_e$ caused by the open crack propagation, the frictional energy $W_w$ caused by the initial microcracks, and the work done $W_l$ caused by the uniaxial compressive stress [33].

The work done $W_l$ can be expressed as:

$$W_l = 4ab(\sigma_1 \varepsilon_1 + \sigma_2 \varepsilon_2) = 4ab S_{11} \sigma_c^2$$  \hspace{1cm} (33)$$

where $2a$ and $2b$ are the lengths of the selected unit, which is the same order of magnitude as the crack length.

Furthermore, the stress intensity factor of the crack array could be calculated according to the existing studies [33]:

$$\left\{ \begin{array}{l} K_I = F_1 \sin \theta \sqrt{w} \\ F_1 = 2c \tau_m \\ \tau_m = \frac{1}{2} \sigma_c \sin 2\theta - \frac{1}{2} \mu [\sigma_c - \sigma_c \cos 2\theta] \end{array} \right.$$  \hspace{1cm} (34)$$

where $\tau_m$ is the shear stress leading to the initial crack slip, $\mu$ is the friction coefficient, and $F_1$ is the shear force that caused the initial crack to slip.

$$l_n = l_{n-1} + l_1 \Delta t$$  \hspace{1cm} (35)$$

where $l_1 = 0.27 \sigma_1$, $l_1$ is used to correct the small crack length. $l_n$ is the length of the $n$th impact cracks, and $2w$ is the spacing between microcracks. Even in the early stage of crack propagation, the calculation results of the stress intensity factor could be kept accurate [36].

Paris fatigue crack propagation formula can be used to describe the relationship between the stress intensity factor from the crack tip and the crack propagation rate:

$$l_n = \frac{dl}{dn} = C_1(\Delta K)^m$$  \hspace{1cm} (36)$$

where $n$ is the number of stress cycles and also is the number of cutting of each tooth; both $C_1$ and $m_1$ are material constants, which can be obtained by fitting according to experimental data.

The tool fatigue damage is divided into the process of crack initiation and crack propagation, as shown in Fig. 6. The crack propagation rate is denoted by $dl/dn$, and the control parameter of crack propagation rate is denoted by the stress intensity factor $\Delta K$. In region I, the value of $\Delta K$ is far less than the fatigue crack threshold value $\Delta K_{th}$, so tool materials have no crack propagation, but with the continuous damage of tool material, crack propagation strength will also continue to increase; when it is more than the critical value, the crack will enter into the propagation stage. At this stage, $dl/dn$ is approximately proportional to $\Delta K$. In region II, since $dl/dn \Delta K$ increases with an approximately constant change rate, the cracks within this region are in a state of stable propagation. When $\Delta K$ continues to increase and the maximum stress at the crack tip exceeds the critical propagation stress, the crack propagation rate increases sharply and the crack propagation becomes unstable. The macroscopic manifestation is that the material can no longer maintain its own physical properties at this time and the material will fracture rapidly.

According to the above, it could be known the variation trend of crack length with the number of cyclic loading, as shown in Fig. 6. The relationship between crack propagation rate $dl/dn$ and stress intensity factor $\Delta K$ is described by Paris formula [5]:

$$\frac{dl_n}{dn} = 8.3112 \times 10^{-16} (\Delta K)^{10.71}$$  \hspace{1cm} (37)$$

where $dl_n/dn$ is the crack propagation rate of tool material under the $n$th impact.

Fig. 6 The relationship of $dl/dn-\Delta K$.
\begin{align}
W_e &= 2f_0 \frac{(k + 1)(v + 1)}{4E} K^2 dl \\
W_f &= 2c\tau_f \chi
\end{align}

where \( \tau_f \) is the shear traction on the crack surface caused by uniaxial compressive stress, and \( \chi \) is the sliding distance of the initial microcrack faces [37, 38].

\[
\chi = \frac{(k + 1)(v + 1)}{4E} c \tau_f \left[ \frac{\pi (l_n + l_1)}{w} \right]^{1/2} \left[ 2\pi (l_n + l_1) \right]^3
\]

\[
\tau_f = \frac{1}{2} \mu [\sigma_v - \sigma_v \cos 2\theta]
\]

where \( l_0 \) is the initial damage value of tool material, and \( l_0^* \) is the initial microcrack propagation rate. After tool material is subjected to \( n \) impacts, the damage value of tool material is \( D_n \) and the crack propagation length is \( l_n \). Determine the damage value \( D_n \) and crack opening propagation length \( l_n \) which is the initial condition when tool material is subjected to \( (n+1) \) impacts.

\[
2W_e + W_f = W_1
\]

Substitute Eqs. (28) and (33) into Eq. (37) to get \( S_{11} \) and the damage value and the expression after the damage are obtained.

### 3.2 The initial and critical damage value of tool material with interval method

Since the crack-related parameters in the initial damage state cannot be accurately determined, the interval method is adopted in this paper to cover the length value of the crack length in the initial damage state as much as possible, and the interval method is used to solve the damage value and the degenerated elastic modulus obtained above.

Cemented carbide is made of cobalt (Co), nickel (Ni), molybdenum (Mo), and other elements bonded to micron powder of high hardness metal and processed at high temperature. According to existing studies, it can be assumed that \( 2c = d \), where \( d \) is the particle size of tool material. Sandvik classified the particle size of hard alloy crystal as shown in Table 1. The particle size of the cemented carbide used in the experiment is medium to fine, about 1 to 2 \( \mu \)m. Since the angle \( \theta \) of the propagation direction of all microcracks is taken as \( \pi/4 \), the interval method [37] is used to calculate the initial crack spacing of \( 2w \), which is 4 times as many as the crack length.

The interval number represents a number set, and the maximum and minimum values of the number set are used as endpoints to describe the interval number. The maximum and minimum values of the real number set are expressed by \( \overline{X} \) and \( X^- \) in the number set, where \( X^- < X^- \), then the bounded real number set can be expressed as [39, 40]:

\[
X^1 = \left[ X^- \overline{X} \right] = \left\{ X \left| X \in \mathbb{R}, X^- \leq X \leq \overline{X} \right. \right\}
\]

The calculation of interval number is based on the algorithm of the set. The addition and subtraction of two interval numbers are added and subtracted between all elements in the two interval numbers. The upper and lower bounds of the interval number obtained by multiplying the interval numbers are respectively the maximum and minimum values of the two interval numbers’ endpoints.

\[
X^1 + Y^1 = \left[ x^- \overline{x}, y^- \overline{y} \right] + \left[ y^- \overline{y} \right] = \left[ x^- + y^- \overline{x} + \overline{y} \right]
\]

\[
X^1 - Y^1 = \left[ x^- \overline{x} \right] - \left[ y^- \overline{y} \right] = \left[ x^- - y^- \overline{x} - \overline{y} \right]
\]

\[
X^1 \times Y^1 = \left[ x^- \overline{x} \right] \times \left[ y^- \overline{y} \right] = \left( \min (M), \max (M) \right), M = x^- y^- - x \overline{y} - \overline{x} y^- \overline{y}
\]

\[
X^1 / Y^1 = \left[ x^- \overline{x} / [y^- \overline{y} \right] = \left[ x^- \overline{x} \right] \times \left( \frac{1}{y^-} - \frac{1}{\overline{y}} \right) \left( 0 \leq y^- \overline{y} \right)
\]

In the process of crack propagation, the density of initial crack and the number of initial crack have important effects on the tool damage. The initial crack density is defined as follows:

\[
f_0 = N_0 c^2
\]

\[
N_0 = \frac{4abP}{c^2}
\]

where \( f_0 \) is the density of the initial crack, \( N_0 \) is the number of initial microcracks in the unit area, and \( P \) is the porosity of

| Grain grade | Grain size of the WC (μm) | Grain grade | Grain size of the WC (μm) |
|-------------|---------------------------|-------------|---------------------------|
| Ultrafine   | 0.3–0.5                   | Very fine   | 0.5–0.9                   |
| Fine        | 1.0–1.3                   | Medium      | 1.4–2.0                   |
| Medium coarse | 2.1–2.4                | Coarse      | 3.5–4.9                   |

Table 1 Grain size grading standards of cemented carbide of Sandvik

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cemented carbide.

Cemented carbide belongs to powder metallurgy material, and there is an adhesive which is inevitably the existence of micro-pores and other microscopic defects, so that the tool in the original state has a certain damage, cemented carbide material porosity calculation formula [41]:

\[ P = \left(1 - \frac{\rho}{\rho_0}\right) \times 100\% \]  \hspace{1cm} (50)

where \(\rho\) is the actual density of cemented carbide, and \(\rho_0\) is the nominal density of cemented carbide.

It can be seen from the above formula that the damage value of tool material \(D\) decreases with the increase of crack length. For fixed tool material, although the initial and critical damage of tool material is an inherent property of tool material, it is also influenced by the crack propagation in the tool initial state. The crack propagation speed is determined by the frequency and strength of the external cyclic loading. Because the initial propagation rate is low, the influence of crack propagation speed on the initial damage value and critical damage value of tool material is ignored in this paper.

As shown in Fig. 7, damage value of tool material at the starting point is 0.0175, and the damage value at this point is considered to be tool material initial damage value \(D_0\), which is determined by the properties of tool material. The tool has small defects in the casting process, which leads to a certain amount of damage to the tool in not cutting stage. The damage value is measured by \([0.0175, 0.0314]\), and the initial damage value is extremely small. According to the existing parameters, it can be seen that when the number of impacts on the tool reaches about 20,000 times, damage value of tool material has an obvious rising stage, which proves that tool material reaches a critical value this is between \([0.41, 0.58]\) at this time. There is obvious damage inside tool material, the length of the crack reaches the critical value, and the tool life reaches the limit, the tool damage value is between \([0.41, 0.58]\), and there is a very high possibility of cutting edge fracture at any moment.

### 3.3 Degradation of elastic modulus

The elastic modulus is a physical quantity that describes the material resistance to elastic deformation. The elastic modulus of tool material will vary with the type of material such as hot working technology, cold plasticity, and other factors [30]. When the tool is milling, under the impact of continuous cyclic loading, the crack in tool material expands, which increases the damage degree of tool material and changes the elastic modulus. The degenerated elastic modulus can be expressed by the damage value of tool material:

\[ \sigma_c = E_D \varepsilon = E(1-D)\varepsilon \]  \hspace{1cm} (51)

Milling belongs to intermittent cutting, so the cutting time of each tooth starts from the tool cutting into workpiece to the end of the tool cutting out workpiece. Therefore, the effective cutting time is determined by the cutting speed, cutting entry angle, and cutting exit angle. The effective cutting time of each tool tooth can be expressed as:

\[ \Delta t = \frac{\theta_{ex} - \theta_{en}}{2\pi} t \]  \hspace{1cm} (52)

where \(\theta_{en}\) is the cutting entry angle, \(\theta_{ex}\) is the cutting exit angle, \(\Delta t\) is effective cutting time, and \(t\) is cutting time.

By substituting the above formula, the changing trend of the tool elastic modulus with time in the milling process can be obtained:

As shown in Fig. 8, the elastic modulus presents a downward trend with the number of impacts. Under the existing cutting conditions, when the number of impacts that the tool
bears under the cyclic loading approaches 20,000 times, the elastic modulus shows a sharp decline, which proves that the tool is in a severe damage state at this time.

3.4 The limit fracture conditions with tool cutting impact

The milling process of cemented carbide tool is the intermittent cutting state. In the air stroke into the cutting workpiece state, the tool will be subjected to greater impact; at this time, the cemented carbide tool is limited to low-impact resistance and toughness ability, so that its usable life is significantly reduced. The tool will go through the process of cutting into and cutting out the workpiece. In down-milling, workpiece and tool teeth collide at a certain angle at the beginning of tooth contact workpiece. This process is depicted by a tool-workpiece contact model with a single degree of freedom in italic collision [27]. On the basis of considering the cutting impact force and combining with the milling force model of workpiece material hardness, the cyclic loading that the cemented carbide tool must bear in milling titanium alloy can be obtained.

The magnitude of the tool cutting-in impact force is mainly determined by the relative contact velocity when the tool collides with the workpiece and the depth of the tool cuts into workpiece. Therefore, it is assumed that there is no energy loss during the impact process; that is, the kinetic energy during workpiece contact model with a single degree of freedom in italic collision [27]. On the basis of considering the cutting impact force and combining with the milling force model of workpiece material hardness, the cyclic loading that the cemented carbide tool must bear in milling titanium alloy can be obtained.

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a) The relation between cutting speed, cutting width and the loading of the tool

b) The relation between cutting speed and the loading of the tool under different cutting widths

c) The relation between feed and tool loading under different cutting speeds

d) The relation between feed and cutting width and the loading of the tool

e) The relationship between cutting width and loading of the tool under different feed

f) Relation between feed and tool loading at different cutting widths
changes at different cutting speeds. With 50 m/min as the boundary, when the cutting speed does not reach this boundary, the curve can be regarded as linear curve, and the curve has a slight change. When the cutting speed exceeds this boundary, the curve can be regarded as a quadratic function curve. When the cutting depth is 0.08 mm, there is a rapid rise stage.

There are two planes in Fig. 9 a–i. The upper plane is the ultimate allowable stress when the tool is not damaged, and the lower plane is the ultimate allowable stress that the tool can withstand when reaching the critical state after a period of cutting. As shown in Fig. 9, the ultimate allowable stress that the tool can withstand after damage is significantly reduced, indicating that the crack length inside the tool has extended to the critical state.

According to the impact fracture critical model and the force and damage value of cemented carbide tool in high-speed milling of titanium alloy, the critical value of impact fracture can be determined. Therefore, the impact fracture damage of cemented carbide tool has time-varying characteristics. As shown in Fig. 10, after the predicted cutting time, the tool damage is solved firstly. The ultimate stress loading that the tool can withstand before and after the damage is compared. The loading that the tool can withstand during the

Fig. 9 continued.
Impact fracture of cemented carbide tool and the loading that the tool needs to withstand under different damage states are extracted. According to the graphical curve, the cutting parameters in the safety range can be given. The part below the curve in Fig. 10 is the safety area, in which the tool can be ensured not to break in the predicted tool life time.

As shown in Fig. 11, the three axes of the spatial coordinate system are cutting depth, cutting speed, and feed rate. The three-axis coordinate system is selected to verify the safety curve of the cutting process of tool material, which can effectively avoid the occurrence of impact fracture in the specified time.

3.5 Experimental verification of tool breakage analytical model

The three-axis CNC machine tool VDL-1000E produced by Dalian Machine Tool Group is used in the experiment, and the dry milling method is used. The experimental tool is flat bottom uncoated end mill, which material is YG6 cemented carbide; the force is measured by Kistler 5236B rotary force transducer.
Using the milling tool, tooth number $N$ is 4, diameter $D$ is 10mm, and spiral angle $\lambda_s$ is 35°; the experimental workpiece material is titanium alloy Ti6Al4V for aviation, and the size is 100×100×100mm; the side milling parameters are shown in Table 2.

The test site diagram of milling titanium alloy with cemented carbide end milling tool is shown in Fig. 12.

The cutting parameters can be selected from the text to ensure the reliability of the tool under large cutting parameters. In order to realize the effective value of the critical cutting parameters of impact fracture more effectively, and make the range of the shadow part within the range specified by the three curves. The tangent of the curve is drawn, and the safety area is verified by different parameter points in Table 2, as shown in Fig. 13.

As shown in Fig. 13, the safety curve of tool material after damage is further divided. When the cutting speed is determined to be 83 m/min, according to the corresponding safety area, the maximum cutting width can be obtained about 2 mm. When the cutting speed is determined to be 83 m/min, the maximum value that can be achieved according to b in Fig. 10 is about 0.13mm/z. At this time, the cutting parameters are selected: cutting speed 70m/min, feed 0.1mm/z. In order to verify the rationality of the cutting parameters, the corresponding experimental cutting parameters are substituted into Fig. 13, and the landing point is in the safety area. It shows that the selected cutting parameters are safe and can effectively avoid the occurrence of impact fracture in the specified time.

The parameters in the shadow can ensure that the cemented carbide tool does not break 200$\mu$m in the predicted tool life which achieved 300s in Fig. 13. As shown in Fig. 13, with the cutting process, the tool continues to be damaged, and safety range of tool cutting parameters will be reduced. According to the cutting parameters, the prediction safety area of tool breakage is verified. The selected tool breakage measurement position is 0.5 times the depth along the end mill to participate, and the measurement is carried out along the flank breakage direction. A super deep scene 3D microscope was used to observe the tool breakage area, and typical breakage morphologies

### Table 2 The experimental cutting parameters

| Experimental samples | $v$ (m/min) | $a_e$ (mm) | $a_p$ (mm) | $f_z$ (mm/z) | Failure time(s) | Damage length($\mu$m) |
|----------------------|-------------|------------|------------|--------------|----------------|---------------------|
| 1                    | 130         | 1          | 3          | 0.15         | 94.2           | 201                 |
| 2                    | 100         | 1          | 3          | 0.15         | 117            | 214                 |
| 3                    | 80          | 1          | 3          | 0.15         | 321            | 241                 |
| 4                    | 80          | 1          | 3          | 0.08         | 313            | 200                 |
| 5                    | 80          | 1.5        | 3          | 0.08         | 207            | 213                 |
| 6                    | 47.12       | 1          | 3          | 0.08         | –              | –                   |

![Fig. 12 The test site diagram](image-url)
were selected and numerically calibrated. As shown in Fig. 14, no. 1, 2, and 5 experimental parameters are located outside the safety range. Severe damage has occurred in less than 300s, and the breakage length along the flank reaches 200μm. No. 3 and 4 experimental parameters are located in the upper and lower boundaries of the safety experimental parameters, and the breakage length along the flank reaches 200μm in the continuous cutting of about 300s. No. 6 experimental parameter is located in the range of safety parameters, and there is nothing broken. The tool damage evolution is characterized in the form of tool flank breakage.

Due to that the matrix of cemented carbide tool is powder metallurgy, cracks and defects are randomly distributed. Because the intermittent cutting is a complex process in the cutting process, the mechanical shock and thermal shock on the tool randomly occur, which causes the fatigue propagation of the crack core inside the tool and finally leads to tool breakage. There is a certain contingency in the occurrence of tool breakage. In addition, the interval coverage of the initial value will have a certain impact on the final prediction results. Cutting edge chipping caused by random impact fracture during the evolution of tool damage is the main failure form of tool fatigue breakage.

4 Conclusion

The large thermal-mechanical loading leads to serious cemented carbide tool breakage in the milling of titanium alloy, which also caused the low machining efficiency. This paper studied the material damage model of cemented carbide tools with high-speed milling of titanium alloy, and studies the influence of tool damage of tool breakage from the perspectives of theory, experiment, and simulation. It is found that the tool exhibits the characteristics of damage accumulation and sudden fracture. The critical condition of tool impact fracture is established, and the conclusions are as follows:

1. According to the research results of the tool damage mechanism, the tool damage accumulation will also occur when the tool cuts into the workpiece, and tool material fracture is more likely to occur. The tool-workpiece contact model is introduced as the single degree of freedom
italic collision model to describe the cutting impact in the milling process.

2. The occurrence of damage accumulation can be attributed to the propagation of the microcracks between the cutting tool material grains. Based on the continuum damage mechanics, the interval method is adopted to cover the uncertain parameters in the model of the initial crack length, which eventually get the tool damage evolution and crack length and curves of relationship between carbide materials. The initial and critical damage values of cemented carbide material are [0.0175, 0.0314] and [0.41, 0.58] respectively. It is also found that when the crack length reaches a threshold value, the damage values of tool material rise rapidly, and the elastic modulus decreases sharply.

3. According to the cutting tool under cyclic loading characteristics and damage mechanics, the impact fracture limit condition is established including cutting parameters and material hardness of the end mill, the degree of the end mill breakage is defined, and the safety area of the tool breakage is divided, which provide support for optimizing cutting parameters of high efficiency milling of titanium alloy.

### Nomenclature
- $h$: The thickness of the undeformed chip; $A_n$: The cutting width; $\sigma_0$: The shear stress at the shear exit; $\psi_0$: The normal stress; $\phi_0$: The normal shear angle; $F_{nm}$: The normal pressure; $F_s$: The shear force; $F_f$: The frictional force; $F_P$: The positive pressure; $H_v$: The Rockwell hardness of the material; $L_1$: The bonding zone length of the rake face; $L$: The tool-chip contact length; $V_c$: The chip velocity; $T$: The average temperature on the rake face; $T_m$: The melting point temperature of the workpiece material; $V_B$: The flank wear volume; $V_B^*$: The constant width of elastic contact zone; $\tau_1$: The maximum shear stress of flank face; $\sigma_1$: The maximum normal stress of flank face; $\mu$: The friction coefficient; $\alpha_n$: The normal rake angle; $\alpha_c$: The chip outflow angle; $\lambda$: The inclination angle; $F_{tw}$: The tangential force on the flank face; $F_{rm}$: The radial force on the flank face; $d_{tw}$: The tangential force; $d_{rm}$: The radial force; $R$: The radius of end mills; $F_{nt}$: The normal contact force; $F_{nt}$: The tangential contact force; $R_1$, $R_2$: The effective collision radii of two collision objects; $u_1$, $u_2$: The Poisson's ratio of the matrix material; $K_0$: The stiffness coefficient; $e$: The deformation of the collision object; $\xi$: The index of penetration depth; $\delta$: The relative velocity of the two objects; $u$: The hysteretic damping factor; $\varepsilon$: The ratio of normal relative velocity; $E_{st}$, $E_{e2}$: The three-dimensional elastic modulus; $F_{im}$: The impact force in X direction; $F_{im}$: The impact force in Y direction; $A_n$: The material unit area; $D$: The damage value of tool material; $\psi_0$: The contact angle during the collision; $\sigma$: The cutting equivalent stress; $\sigma$: The triaxial stress; $v$: The Poisson's ratio of tool material; $\sigma_1$: The uniaxial compressive stress; $\varepsilon_r$, $\varepsilon_f$: The elastic strain of the non-damaged tool material; $\Delta \varepsilon_1$, $\Delta \varepsilon_2$: The elastic strain of the non-damaged tool material; $\sigma_1$, $\sigma_2$: The normal stress in two directions; $W_c$: The elastic strain energy caused by the open crack propagation; $W_f$: The frictional energy caused by the initial microcracks; $W_l$: The work done caused by the uniaxial compressive stress; $E$: The elastic modulus of tool material; $K$: The parameter determined by Poisson's ratio; $K_0$: The stress intensity factor of the crack array; $l_0$: The length of the nth impact cracks; $2W$: The spacing between microcracks; $n$: The number of stress cycles or the number of cutting of each tooth; $AK$: The stress intensity factor; $\tau_0$: The shear traction on the contact surface; $\chi$: The sliding distance of the initial microcrack faces; $l_0$: The initial damage value of tool material; $l_0'$: The initial crack propagation rate; $l_0$: The crack propagation length; $D_n$: The damage value of tool material; $f_0$: The density of the initial microcrack; $N_0$: The number of initial microcracks in the unit area; $P$: The porosity of cemented carbide; $\rho$: The actual density of cemented carbide; $\rho_0$: The nominal density of cemented carbide; $2a$, $2b$: The lengths of the selected unit; $c_0$: The length of initial microcracks; $l$: The length of each open crack; $\theta_{en}$: The cutting entry angles; $\theta_{ex}$: The cutting exit angle; $\Delta t$: The effective cutting time; $t$: The cutting time; $F_{zone}$: The overall impact force; $G$: The shear modulus of cemented carbide tools; $\gamma$: The shear strain of cemented carbide; $A$: The cross-sectional area of the fracture; $V_Bp$: The width of tool flank breakage; $l_p$: The cutting depth; $\xi$: The impact stress; $[\rho]$: The allowable stress; $E_{mp}$: The elastic modulus after tool damage.

### Code availability
Not applicable.

### Author contribution
Caixu Yue put forward the theme of the paper and established the structure of the paper; Yanjie Du established the damage model and processed the simulation data; Xiaochen Li verified the model, and processed the experimental data; Xianli Liu and Steven Y. Liang examined the overall structure of the paper and made suggestions on the details of the paper.

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### Data Availability
The datasets used or analyzed during the current study are available from the corresponding author on reasonable request.

### Declarations

#### Ethics approval
The content studied in this article belongs to the field of metal processing, and does not involve humans and animals. This article strictly follows the accepted principles of ethical and professional conduct.

#### Consent to participate
My co-authors and I would like to opt in to In Review.

#### Consent for publication
I agree with the Copyright Transfer Statement.

#### Competing interests
The authors declare no competing interests.

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