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Toward a new tribological approach to predict cutting tool wear

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ABSTRACT

This work aims at improving the numerical modelling of cutting tool wear in turning. The key improvement consists in identifying a fundamental wear model by means of a dedicated tribometer, able to simulate relevant tribological conditions encountered along the tool–workmaterial interface. Thanks to a design of experiments, the evolution of wear versus time can be assessed for various couples of contact pressure and sliding velocities (σ_n, V_s) leading to the identification of a new wear model. The latter is implemented in a numerical cutting model to locally simulate tool wear along the contact with regard to each local tribological loading.

1. Introduction

Tool wear remains of high interest for industry, as it influences process costs and part's surface integrity. Although experimental and analytical investigations have been the main ways to investigate wear, the growing development of computers and Finite Element (FE) methods now enables predicting tool wear based on chip formation simulations. The contribution of a large number of papers has been summarized in Ref. [1]. Whereas most of the models are concerned with 2D orthogonal cutting operations [2–5], some recent contributions have started to predict wear in 3D cutting operations [6,7]. The models start with a chip formation simulation, providing the tribological conditions along the tool–workmaterial interface. The local material loss is then simulated by the displacement of nodes along a defined direction based on the local wear rate computed according to the local thermo-mechanical loadings after a unit time. This results in an updated tool geometry that is imported back into a new chip formation simulation, generating a closed loop. These models predict tool wear thanks to an iterative procedure. However, there are two critical issues related to this approach.

The first one concerns the lack of knowledge of friction, heat partition, and thermal contact conductance along the contact, i.e. depending on local tribological conditions, as pointed out by [3,8–10]. These three directly control the interface temperature and small variation may have a drastic effect. For instance, Ref. [10] revealed that varying the thermal contact conductance from 10^4 to 10^8 W/m^2 K can change the interface temperature from 300 °C to 900 °C when cutting an AISI1045 steel. The major problem is that there is no direct method to assess the thermal contact conductance and its evolution all along the tool–workmaterial interface, as it is strongly dependent on the local contact pressure. This high sensitivity of interface temperature is especially high when simulating short cutting durations, as a thermal steady state can not be reached. When simulating long duration cutting operations, and by the way reaching a thermal steady state, the sensitivity to the thermal contact conductance becomes less significant. As a consequence, at the moment, a large uncertainty remains for the temperature values that can be simulated at the tool–workmaterial interface, especially when simulating short cutting durations.

The second critical issue concerns the wear model. Indeed, most of the papers on the topic focus on the numerical methods and not on the wear models themselves. They consider the preliminary works of Refs. [11,12], who proposed equations based on the sliding velocity V_s, interface temperature T and contact pressure σ_n. For instance, as in Ref. [11],

$$\frac{\partial W}{\partial t} = a\cdot\sigma_n\cdot V_s\cdot\exp\left(-\frac{b}{T}\right) \tag{1}$$

The parameters a and b are then determined using an inverse approach based on cutting tool wear tests. The major drawback of this method is that these parameters are directly dependent on the implementation of the whole numerical model (including its input data, such as friction, heat partition, thermal contact conductance, workmaterial constitutive equation) and on the FE code (Abaqus, Deform, integration scheme, element type . . .) used for the determination. Thus, transferring the identified wear model to any other numerical model, e.g. to another laboratory or to another end-user, becomes highly questionable. Moreover, any improvement of the input data leads to another set of parameters. So it becomes clear that in the current state of the art, a wear model

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should be identified independently of the cutting process, and rather with friction wear tests performed under relevant tribological conditions considering the rake face and flank face of the tool in cutting. The present paper will thus aim at (i) precisely defining these conditions, (ii) identifying a wear model thanks to the open tribometer proposed by Ref. [13] (Fig. 1) with a consistent range of sliding speeds and contact pressures, (iii) implementing it in a multi-step cutting simulation (Fig. 1) able to reach a thermal steady state, and (iv) updating the tool geometry accordingly with an iterative procedure.

NB: This approach, as well as the previous works [3–7,9,11,12], consider progressive wear phenomena (abrasion, diffusion, adhesion) and do not consider cracks (fatigue) [14] as these local phenomena (microscopic scale) are much more difficult to predict and as a consequence to model at a macroscopic scale as in the present cutting process.

The case study will consider AISI304L stainless steel machined with a WC-Co tool coated with a TiN layer in dry conditions and a cutting speed of 260 m/min and a feed of 0.1 mm/rev.

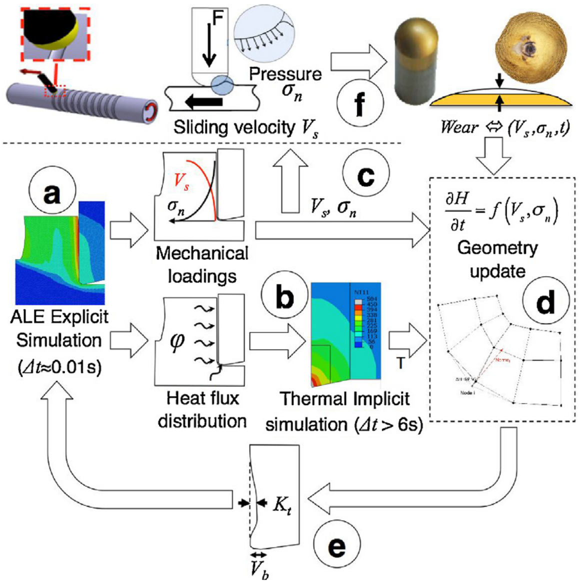

Fig. 1. Principle of the wear model identification and its integration in the muti-step tool-wear modelling procedure.

2. Identification of the tribological conditions along the tool–workmaterial interface

Before starting the identification of any wear model, a clear determination of the tribological conditions has to be performed. The method is based on an orthogonal cutting simulation (ALE) developed with Abaqus Explicit, already described in detail in Refs. [10,13]. The tool is considered as thermo-rigid and the flow-stress model of the 304L is taken from Ref. [15]. Both the friction and heat partition models depending on the sliding velocity have been identified thanks to the open tribometer described in Ref. [13], and enable a better description of the thermo-mechanical phenomena at the tool–workmaterial interface, i.e. depending on local tribological conditions. These models have been implemented through a user subroutine VUINTER®. The thermal contact conductance has been assumed to be 10^4 W/m^2 K all along the contact.

Based on the current computational power, these numerical simulations only simulate some milliseconds of the real cutting process. Within such a very short time, the temperature cannot reach a steady state. Nevertheless, several authors, such as Ref. [16], have already underlined that the mechanical parameters (forces, chip formation, contact length) can stabilize (Fig. 1c). The heat flux φ induced by friction and plastic shear deformation at the interface can be considered as stable. The heat flux density distribution at the cutting tool interface is thus extracted from the ALE simulation and a second pure thermal simulation of the heat transfer in the tool is performed with ABAQUS Implicit (Fig. 1b). It is then possible to predict the temperature field in the tool after several seconds of cutting. Typically, the thermal steady state can be reached after 5–6 s of cutting.

After these two steps, the tribological conditions (stresses, velocity, temperatures) along the tool–workmaterial interface are determined. Fig. 2 plots an original way of presenting the tribological conditions faced by the tool. The red curve represents the evolution of the normal pressure along the contact. The black curve indicates the velocity and highlights that relative movement is only possible at the end of the contact on the rake face (zone ‘a–b’ where the crater wear may appear) and on the flank face (zone ‘c–d’ where the flank wear appears). The blue curve plots the evolution of the temperature in the tool. As discussed previously, this temperature can definitely be questioned. However, it can be observed that its evolution is rather smooth, without any sharp gradient. This is the consequence of the long simulated cutting time, which enables heat diffusion and temperature homogenisation.

A second original way of presenting the tribological conditions at the interface is presented in Fig. 2. This emphasizes the combinations of ‘contact pressure/sliding velocity’ occurring along the contact. This figure provides the range of tribological conditions that should be used to identify a wear model with a new tool geometry. The next section will present the identification procedure.

3. Identification of a wear model

The identification of a wear model, independent of the cutting process, requires an open tribometer providing the relevant tribological conditions as proposed by Ref. [13]. The tribometer principle is mounted on a CNC lathe (Fig. 1f). The friction at the interface between the cutting tool and the machined material is estimated by the friction of a pin with a spherical geometry, made of a similar grade as the cutting tool (i.e. WC-Co + TiN Coating), that rubs against a cylindrical bar made of the same grade as the machined material (i.e. 304L). The friction velocity is defined by the spindle of the lathe, whereas the contact pressure is controlled thanks to the combination of the pin’s diameter and the normal force applied by the hydraulic jack.

As it is not possible to test all the tribological conditions reported in Fig. 2, a selection of some significant testing conditions has to be made. It is well known that the two most critical wear zones are the crater wear on the rake face (zone a $\geq$ b in Fig. 2) and the flank wear (zone c $\geq$ d in Fig. 2). So wear tests should be performed with tribological conditions in accordance with these critical zones. As shown in Fig. 2, the range of contact pressure is between 1 and 2 GPa and the range of sliding velocity is from 1 to 4.3 m/s.

The role of temperature has to be discussed. It is obvious that temperature plays a key role in wear mechanisms. However, many standard tribological tests are performed under rather smooth contact conditions and the temperature in the contact is commonly induced by an external heating system. On the contrary, the tribological conditions in cutting are extremely severe, leading to a large plastic deformation in the bulk of the chip and a very high local heat generation. The temperature is a consequence of these extreme tribological conditions and can neither be predetermined (same scientific issue regarding the thermal contact conductance at the interface between the pin and the bar), nor measured, as the gradient is very high in a very narrow zone. Moreover, as stated by Ref. [17], there is a strong link between the contact temperature and the sliding velocity in this tribotest. So it is questionable if both parameters (T and V_s) can be considered at the same time in the wear model. In the present work, the authors attest that it is not realistic to consider the contact temperature as a parameter in the

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