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Toward a better understanding of tool wear effect through a comparison between experiments and SPH numerical modelling of machining hard materials

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The aim of this study is to improve the general understanding of tungsten carbide (WC–Co) tool wear under dry machining of the hard-to-cut titanium alloy Ti6Al4V. The chosen approach includes experimental and numerical tests. The experimental part is designed to identify wear mechanisms using cutting force measurements, scanning electron microscope observations and optical profilometer analysis. Machining tests were conducted in the orthogonal cutting framework and showed a strong evolution of the cutting forces and the chip profiles with tool wear. Then, a numerical method has been used in order to model the machining process with both new and worn tools. The use of smoothed particle hydrodynamics model (SPH model) as a numerical tool for a better understanding of the chip formation with worn tools is a key aspect of this work. The predicted chip morphology and the cutting force evolution with respect to the tool wear are qualitatively compared with experimental trends. The chip formation mechanisms during dry cutting process are shown to be quite dependent from the worn tool geometry. These mechanisms explain the high variation of the experimental and numerical feed force between new and worn tools.

1. Introduction

Aeronautic titanium alloys are widely used in aircraft industry for applications requiring an excellent mechanical resistance at high temperatures. However, the main problems encountered during the cutting process are the low material removal rates and the short cutting tool life. The low thermal conductivity (about 15 W/m K against 270 W/m K for a steel CRS1018 at 700 °C), together with a high chemical affinity for many tool materials often cause a premature wear. Generally, the machining of titanium alloys is carried out using lubricants. Lubrication improves machinability of the workpiece material, increases productivity by reducing the tool wear and extends the tool life. Nevertheless, reduction or suppression of the lubricant is increasingly needed to agree with the urgent economic and ecological requirements. The machining process in dry conditions becomes unavoidable but is however still far from being optimised.

In this study, the Ti6Al4V alloy machining is analysed under dry conditions. Ti6Al4V is hard to machine and among the tool materials usually used (high speed steel, carbide, ceramic, CBN, diamond), the carbides are economically recommended for the titanium alloys machining process [1–3]. Indeed, the high speed steels are misadvised because of their severe plastic deformation at high temperatures. The ceramic tools are not recommended for machining of titanium alloys because of their weak performance due to a severe wear rate caused by a great chemical reactivity with titanium particles. Moreover, their mechanical and thermal shock resistance is known to be low [4]. On the contrary, super hard cutting tools in cubic boron nitride (CBN) or polycrystalline diamond (PCD) show a good performance in terms of wear rate when machining of titanium alloys. Although cutting speeds until 150 m/min can be achieved, they are not much used because of economical aspects [5]. However, under dry conditions, these two materials (PCD and CBN) can react with titanium leading to severe wear. Dearnley et al. [6,7] showed that straight grade K carbide represents actually the best choice for machining titanium alloy. This tool material is thus used in this study.

Many experimental studies are focussed on wear analysis. This approach implies various measurement difficulties mainly due to the cutting process speed. The implementation of these experiments thus requires heavy experimental means. The data relatively easy to access are the cutting forces and the chip morphology. Recent progress gives access to the fields of temperature and deformation [8]. Nevertheless, these techniques are still not very accessible and give not very precise results.

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So in order to overcome experimental limits, since many years, significant effort has been devoted to the development of computational models of high speed machining. Most numerical models are based on Lagrangian or Arbitrary Lagrangian Eulerian (ALE) Finite Element Methods (FEM) [9,10]. These approaches imply two major difficulties. Firstly, the friction model must account for all the tribological complexity of machining (dynamic friction). In most cases, the Coulomb model is used in cutting models despite its simplicity and limitations [10]. Secondly, the workpiece/chip material separation is difficult to consider. Lagrangian FEM presents the drawback of leading to large mesh distortions. This implies the use of ALE methods and remeshing techniques which are known to be time consuming and to loose energy through a loss of stresses during remeshing.

The evolution of the cutting forces and the morphology of titanium alloy chips are experimentally analysed. The cutting tool damage modes are identified using an optical profilometer and a scanning electron microscope (SEM).

A numerical meshless method, namely the Smoothed Particle Hydrodynamics (SPH) method, is used to model the machining process with a new and a worn tool, respectively. SPH is a meshfree Lagrangian method. Material properties and state variables are approximated by their values at a discrete set of disordered points, or SPH particles. The developed model introduces a new approach of material separation and friction in cutting modelling by particles interactions. An original aspect of this study is that the developed SPH cutting model is used as a tool for a better understanding of chip formation with worn tools.

Finally, the predicted chip morphology and the cutting force evolution with respect to the tool wear are qualitatively compared with the experimental results. This allows explaining several mechanical key aspects of the chip formation under dry machining with worn tools.

2. Experimental set-up and results

2.1. Experimental procedure

The orthogonal cutting tests were carried out using a planer machine with a cutting speed about 60 m/min. The planer machine is instrumented with a KISTLER dynamometer to measure the cutting F_c and the feed F_f forces. The workpiece material is a titanium alloy Ti6Al4V whose mechanical and thermal properties are given in Table 1.

The machined part is a $1000 \times 60 \times 50$ mm block whose surface was machined to get grooves around 4 mm in width. In order to avoid a tool/workpiece mechanical impact at the beginning of the cutting process, the workpiece was chamfered. The machining length is constant for each studied insert and equals to 5 m , i.e. 5 passes of 1 m each.

The studied cutting conditions are summarized in Table 2. A null rake angle was chosen because it allows "good" cutting edge strength under dry machining [11]. All the tests were carried out without lubrication.

Table 1
Mechanical and thermal properties of the titanium alloy Ti6Al4V

Tensile strength (MPa)	931
Limit of elasticity (MPa)	862
Elongation (%)	10
Reduction in area (%)	25
Young modulus (GPa)	110
Hardness (HV)	340
Density (g/cm^3)	4.43
Specific heat, 20–100 °C (J/kg K)	580
Thermal conductivity at 20 °C (W/m K)	7.3

Table 2
Cutting conditions

Tool material	Tungsten carbides Grade H13A WC–6Co K20
Rake angle	0°
Clearance angle	11°
Cutting speed	60 m/min
Feed	0.3 mm

2.2. Cutting and feed forces when dry machining titanium alloys

Measured cutting and feed forces are shown in Fig. 1. It can be noticed that the feed forces are raised about 95% between the first pass and the fifth pass. This evolution can directly be linked to the tool/chip contact. Indeed, when using a 0° tool rake angle, the recorded feed force corresponds to friction forces on the tool plan (Apparent friction coefficient is defined in this simple case by $\mu = F_f/F_c$). Therefore, there is a strong dependence between feed forces and tool/chip contact conditions.

The increase in the measured cutting force level and apparent friction coefficient shows that the tool wear increases with the cutting length while the tool–chip contact becomes "a sticking contact". The change in contact condition can be observed in Fig. 2. The chip surface shown in Fig. 2a corresponds to a very slight sticking contact while that shown in Fig. 2b rather looks like an accentuated sticking contact. The cutting forces alone can only give qualitative information on the wear of the cutting tools during machining of the Ti6Al4V.

2.3. Chip formation

The Ti6Al4V titanium alloy is known to generate segmented chips (also named "saw–tooth" chips) very often at relatively low cutting speeds. The chip analyses have shown that the tool wear affects the chip formation. In the studied cases, a thicker chip and a lower frequency of chip segmentation are observed when tool wear is important, see Fig. 3. Moreover, the wear by edge chipping will cause the tool geometry to change during the machining process. The consequence of chipping and adhesion mechanisms is an increase in the force level, especially for the feed force as previously noticed in Fig. 1. Therefore, the wear state of the tool surface affects the chip formation (in terms of segmentation frequency, shear band thickness, etc.) and also the cutting and feed forces.
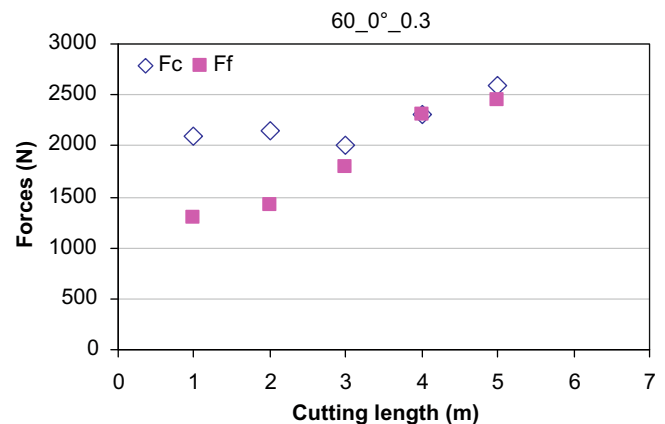

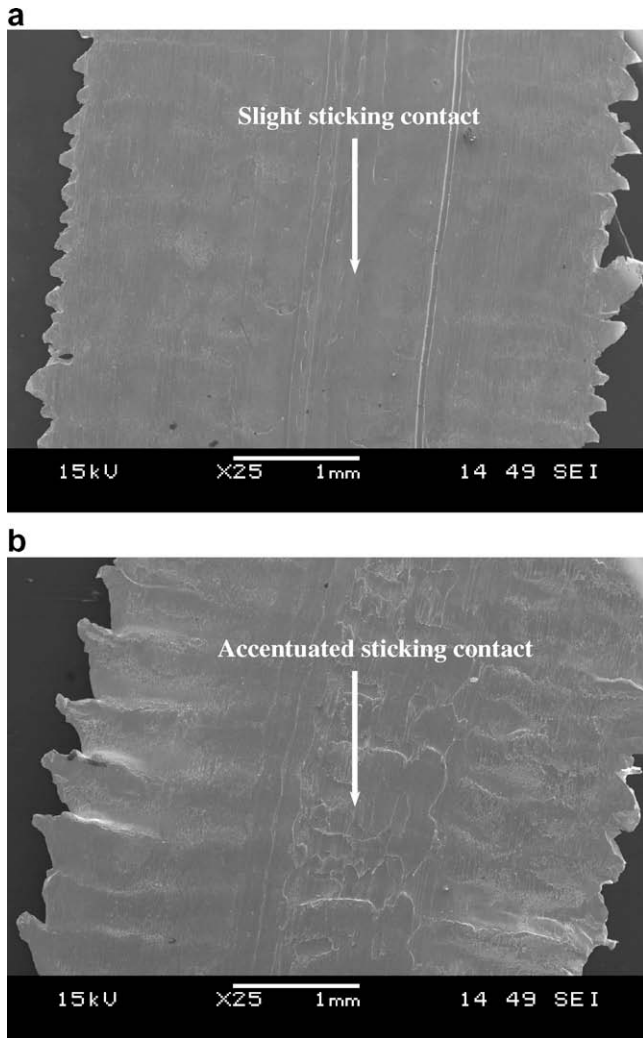**Fig. 1.** Cutting forces evolution with cutting length.

Fig. 2. Chip surfaces in contact with the tool rake face: (a) after a cutting length of 1 m, (b) after a cutting length of 5 m.

2.4. Tool wear

Several investigations of tool surfaces were performed under optical profilometry and scanning electron microscopy (SEM). Fig. 4 shows the rake and flank worn faces of a 0° rake angle tool after 5 m machining. Significant edge chipping and slight titanium deposits are observed.

The basic mechanism of this intense chipping is probably link to titanium adhesion on the tool rake face. As the cutting length increases, the titanium layer is removed from the tool rake face and, at the same time, some tungsten carbide grains can be carried away inside the chip, as shown in Fig. 5. This phenomenon gives rise to chipping and the tool edge becomes weakened (see Fig. 6) and finally fails.

The aim of this study is to improve the general understanding of the chip formation with worn tools. The experimental approach allowed us to observe the consequences of the chip formation with worn tools: cutting forces, chip morphology, tool changes. Nevertheless, it is very difficult to observe experimentally the steps of the chip formation and to obtain precise information. For a better understanding of what happens, we propose to complete the experimental work by using a numerical SPH cutting model. We shall see that this approach allows, among other things, the feed force increase with the tool wear to be explained.

3. SPH cutting model

3.1. Framework

The aim of the numerical approach developed in this study is to overcome some experimental investigation limits and to supplement the knowledge of the chip formation with a worn tool under orthogonal cutting conditions. It is based on the 2D metal cutting model proposed by Limido [12]. This model uses a Smoothed Particle Hydrodynamics (SPH) method in the frame of the LS–DYNA hydrodynamic software [13].

3.2. Basic principles of the SPH method

The Smoothed Particle Hydrodynamics (SPH) method was invented by Lucy [14], Gingold and Monaghan [15] in 1977 for gas dynamics problems in astrophysics and extended by Libersky [16] to treat solid continua 15 years later. Material properties and state variables are approximated by their values on a set of discrete points, or SPH particles. This avoids the severe problems of mesh tangling and distortion which usually occur in Lagrangian analyses involving large deformation. Indeed, grid based methods such as Lagrange and Euler FEM assume connectivity between nodes to build spatial derivatives. SPH uses a kernel approximation which is based on randomly distributed interpolation points without any assumption about which points are neighbours to calculate spatial derivatives (Fig. 7). More details on the SPH formulation for hydrodynamics with material strength can be found in [17] and specific formulations used in LS–DYNA are introduced in [13].

The SPH method applied to machining modelling involves several advantages compared to classical FEM. First, no remeshing is needed when deformations are high. Another advantage induced by the SPH method is the "natural" chip/workpiece separation, i.e. no separation criterion is necessary. The creation of the new free faces is directly managed by the SPH method. In the same way, the SPH method presents an original aspect concerning contact handling. Indeed, it does not require the definition of surfaces. So, it does not involve a friction parameter. Friction is directly managed via particles interactions. These interactions result from the balance of the three conservation equations solved in SPH (mass, momentum and energy). It is very interesting because the Coulomb friction parameter is often used in FEM to adjust predicted cutting forces with the experimental results [9,18]. The results of the (LS–DYNA) SPH model were compared with experimental data in [12].

3.3. Metal cutting model assumptions

Several assumptions were made in order to reduce the model size and the computation time, allowing the development of a useful tool. A 2D plain strain study is allowed by the orthogonal cutting conditions framework. The tool is supposed to be rigid and its velocity is imposed. During the process, the computation time is also reduced by using an imposed tool velocity ten times higher than the real one. This assumption is usually used in simulation of stamping processes. It is valid as long as the accelerated mass is low and when the material behaviour is slightly influenced by the resulting strain rate increase.

Accurate and reliable flow stress models are considered as highly necessary to represent the material constitutive behaviour under high speed cutting conditions. In our study, the constitutive model proposed by Johnson and Cook [19] is used and all associated parameters result from the literature.
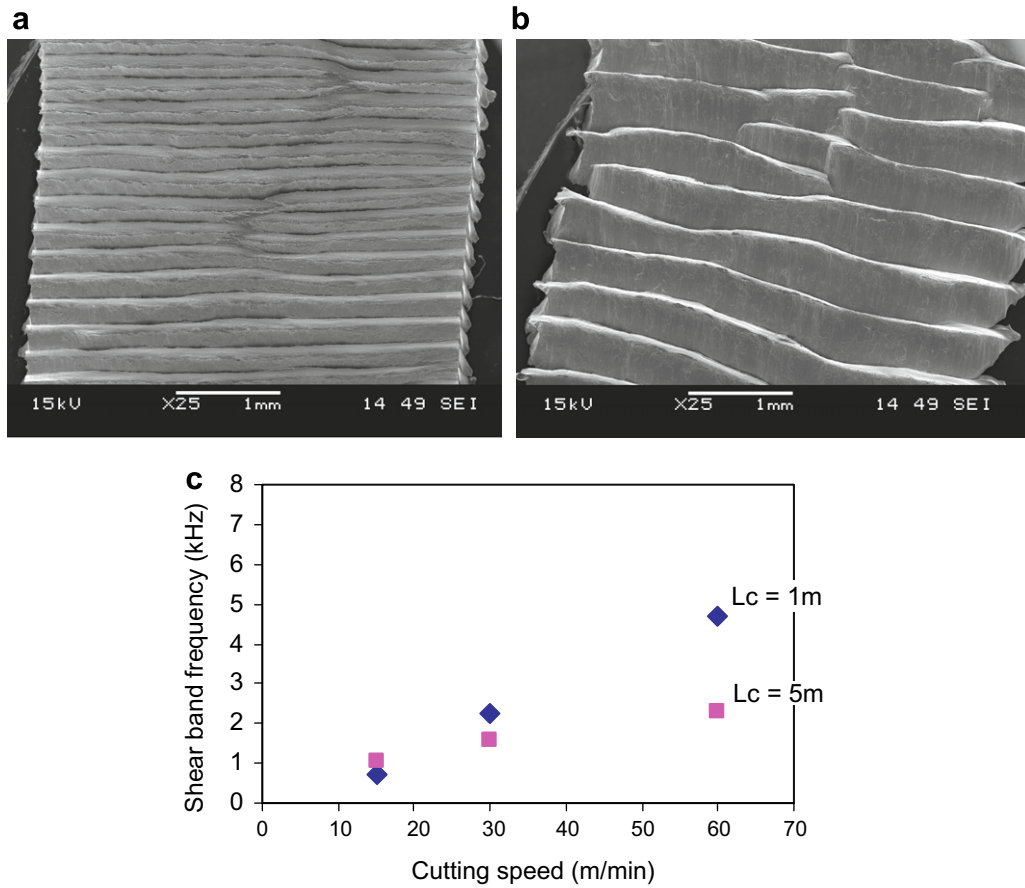
Fig. 3. Free surface of chips obtained after a cutting length (L_c) of (a) 1 m; (b) of 5 m; (c) evolution of chip segmentation frequency vs. cutting speed.
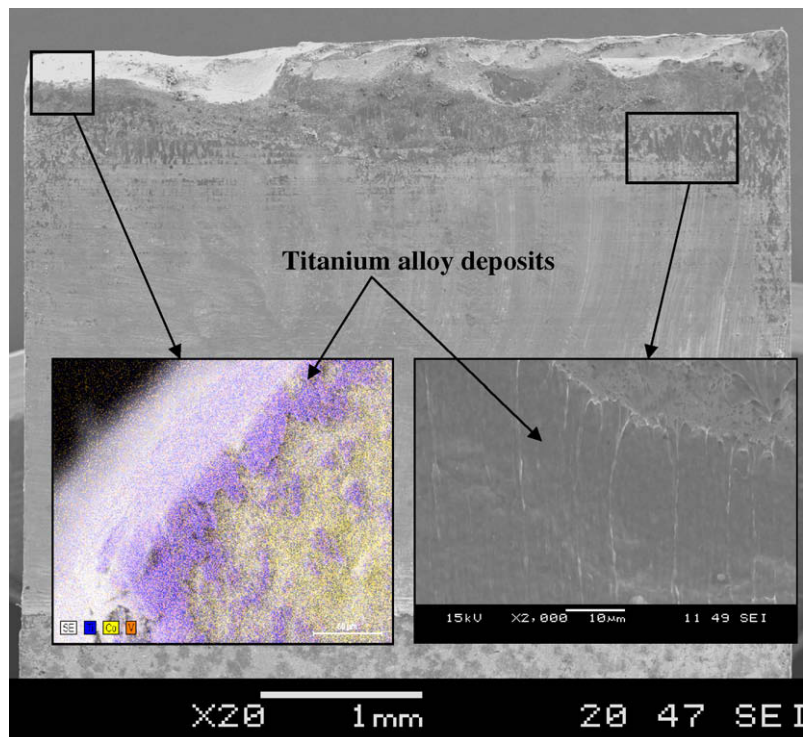

Fig. 4. SEM images of a 0° rake angle tool under a cutting speed of 60 m/min and a feed of 0.3 mm.
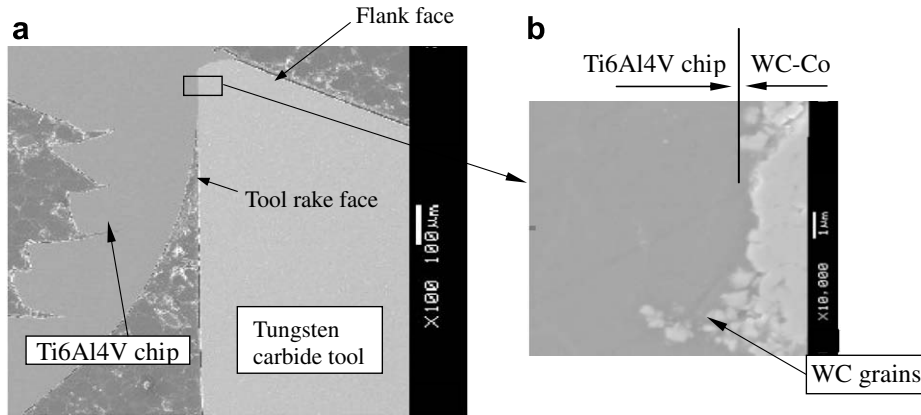
Fig. 5. (a) SEM image of a TA6V chip stuck on the tungsten carbide tool rake face; (b) SEM image showing WC grains into TA6V chip.

4. Tool wear analysis using SPH model

4.1. Proposed approach

The tool wear analysis method developed here is based on a chip formation analysis using experimental and numerical results. The main idea is to use the developed SPH model to study the cutting forces and the chip formation with new and worn tools and compare these results to available experimental data.

As a first step, experimental tests are carried out in order to measure the cutting forces at various steps of tool wear for given cutting condition (Section 2). Then, the experimental test for which the cutting force evolution and the change of tool geometry resulting from the wear were the most significant has been selected for comparison with numerical SPH results. The tool geometry before
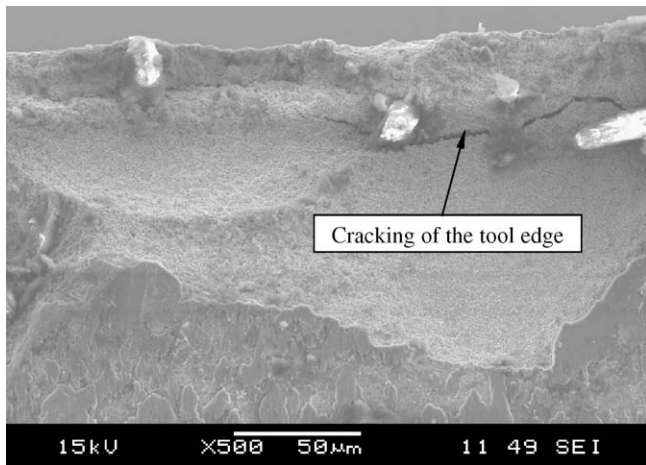

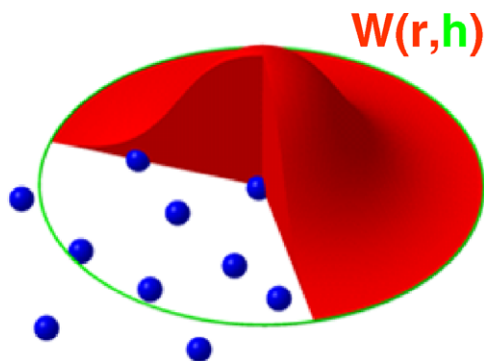**Fig. 6.** Crack propagation after chipping of the tool edge.

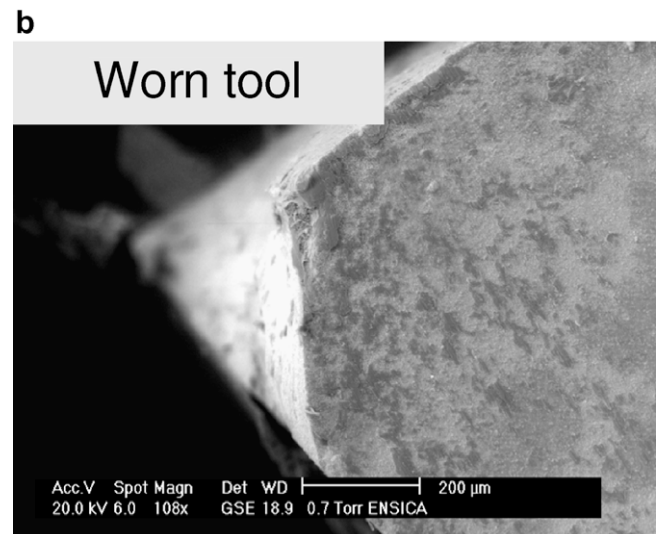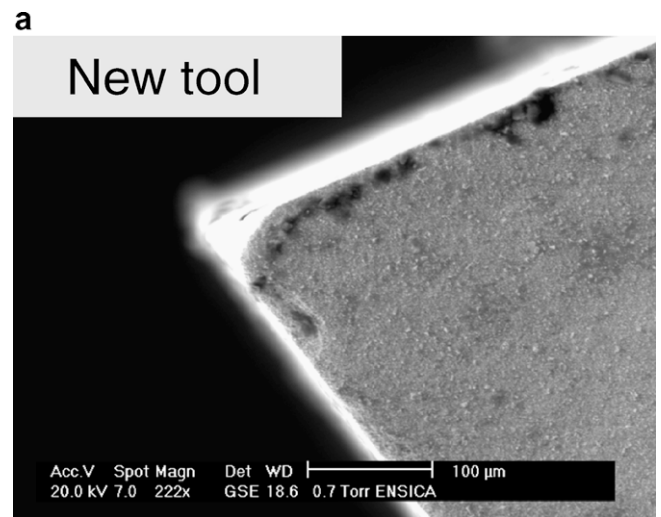**Fig. 7.** Kernel approximation.

Fig. 8. SEM images of (a) a new and (b) a worn tool.

and after machining has been obtained by means of a scanning electron microscope (SEM). These geometries are shown in Figs. 8 a and b.

Finally, two SPH cutting models are studied with new tool edge geometry and worn edge geometry. Then, the chip morphology and cutting forces evolution are compared with the experimental results. One can notice that this approach is developed in the framework of Ti6Al4V machining but could be extended to other metallic materials.

4.2. Chip morphology

Under the cutting speed and feed range studied, Ti6Al4V produces shear localized chips (see Fig. 9). Shear localized chips are characterised by oscillatory profiles. They result from adiabatic shear band formation in the primary shear zone of the workpiece material. The SPH model gives information about the chip formation. Fig. 9a and b show the plastic strain field obtained with a new and a worn tool, respectively, and the shear localization zones. The chip type obtained for the SPH model is in accordance with experiments.

These tests make possible to display the influence of the wear on the appearance of the adiabatic shear bands. As it was already noticed in Section 3, a severe tool wear will change the frequency of adiabatic shear bands (defined as being the number of segments produced per unit time) and also its thickness. The SPH model shows a shear band thickness increase and a decrease of frequency

of chip segmentation with wear, which is also observed experimentally. The distance between two adiabatic shear bands is predicted with an error of about 20%. Nevertheless, the numerical/experimental mismatch is important and cannot be totally due to the thickness experimental uncertainty. This can also be explained by the adiabatic process hypothesis and the absence of a damage model. The temperature is thus overestimated and the smoothing principle of the SPH method propagates it. This tends to increase the temperature in a thicker band and thus a larger shear band is created. This phenomenon is accentuated by the relative lack of particles to represent the adiabatic shear band. Indeed, the distance between two particles is approximately 6 µm and the smallest measured shear band is 21 µm thick. Thus, three particles have to represent the adiabatic shear band development. One can think that an increase in the particle density and the use of a damage model would allow a better approximation of the shear band thickness.

Another original aspect is the identification of a metal dead zone in the worn tool cutting case. The velocity field analysis at the tool tip (Fig. 10) during the cutting process allows to identify a zone where the matter has the X velocity component equal to the tool speed and a null Y component, see Fig. 10b. This area is reduced to a kind of triangular zone in front of the tool tip which moves with the tool. This physical mechanism was already observed in experiments [20] and even conceptualized in analytical cutting models dealing with the chamfered tools [21]. The SPH approach is able to predict this stagnation zone because of its mesh-
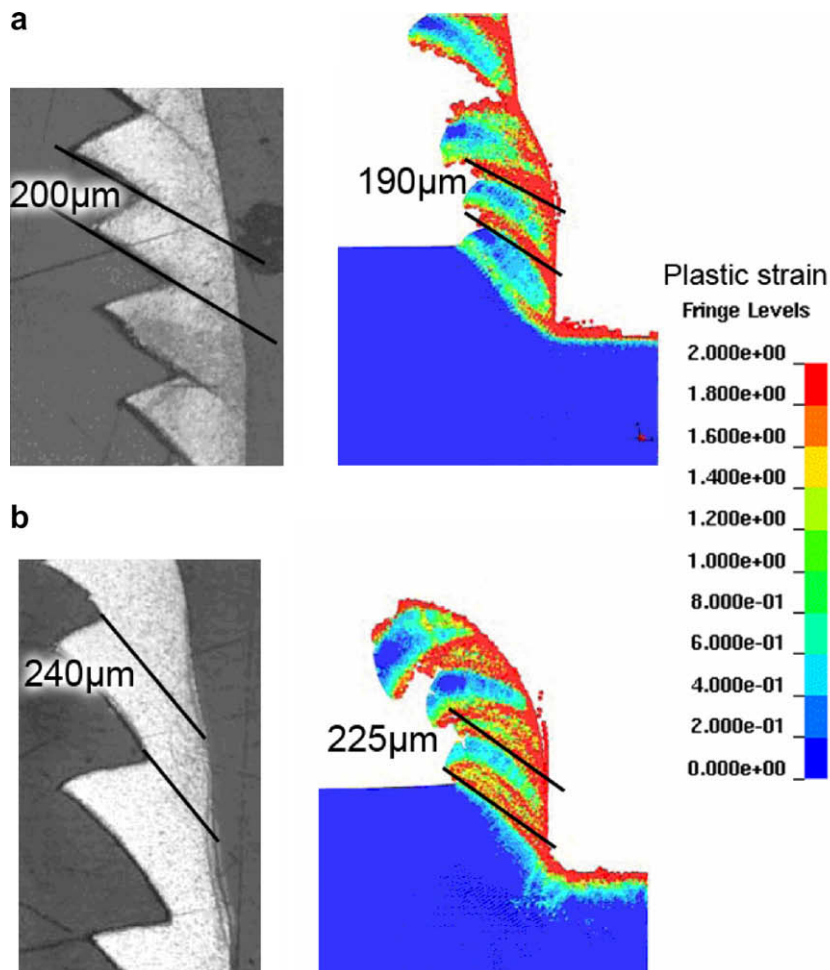

Fig. 9. Experimental/numerical chip formation using (a) new tool and (b) worn tool geometry.

less nature. This allows to model accurately special cases where a high strain rate zone is close to a stagnation zone. The dead zone previously described is one of these special cases which could not be easily described by a classical Lagrangian finite element model.

4.3. Cutting forces

A precise prediction of the cutting forces does not imply in a systematic way the good representation of the physical phenomena. Thus, several numerical tests were carried out in order to check the SPH model validity. Indeed, in metal cutting modelling two main parameters greatly influence the obtained cutting forces: friction parameter and material model parameters.

First, concerning friction management, the choice of a friction model representing the physical phenomena that occur during cutting is a significant part of the machining modelling problem resolution. Indeed, the calculated cutting forces are highly dependent on the friction parameter [18]. Because of its simplicity, the Coulomb model is widely used in classical cutting models. Nevertheless as it should be an input in the models, it is often used in order to adjust the cutting forces obtained by FEM with those from the experimental results [9,18]. On the contrary, SPH friction modelling is based on particles interactions and no friction parameter (like Coulomb parameter) has to be defined.

Second, the flow stress model describing the workpiece material behaviour as a function of temperature, strain and strain rate is considered highly influent to represent the material behaviour under a cutting process [22]. The Johnson–Cook constitutive model [19] used here describes the flow stress of a material by considering a product of terms related to strain, strain rate and temperature, as in the following equation:

$$\bar{\sigma}_y = \left[A + B(\bar{\varepsilon})^n\right]\left[1 + C\ln\left(\frac{\dot{\bar{\varepsilon}}}{\dot{\bar{\varepsilon}}_0}\right)\right]\left[1 - \left(\frac{T - T_{\mathrm{room}}}{T_{\mathrm{melt}} - T_{\mathrm{room}}}\right)^m\right] \qquad (1)$$

Characterisation data are needed to determine its parameters. These data must be representative of the studied material response under machining loading conditions. Experimental data are generally obtained at high strain rates (until $10^3\,\mathrm{s}^{-1}$) and low strains (<0.5) using the split Hopkinson pressure bar method but not in the range observed in cutting regimes. Besides, a large variation of available Johnson–Cook parameters can be found in literature (Table 3). The recent study of Arrazola et al. [23] showed that these parameters have a great influence on the cutting force values. Like for Coulomb parameter, it is possible to obtain a data file which gives the good forces values. During our work, we have found four sets of different Johnson–Cook parameters in the literature for the Ti6Al4V alloy [24–27]. These data are reproduced in Table 3. All these sets were tested in order to check their influence on the results obtained (Fig. 11).

This influence is represented by a numerical error bar on the Fig. 12. We used as reference for analysis the data file suggested by Meyer [24] because it results from a characterization detailed in the high range of strain, strain rate and temperature. Nevertheless, Fig. 11 reveals that the parameters [26] permit to obtain better results.

These two major aspects can explain the relatively important variations of about 25% obtained on the predicted cutting forces, see Fig. 11. Moreover, a factor that can be responsible for the difference between predicted and experimental results is the time scaling that reduces computational time but amplify inertial effects. Another factor, with less influence, is the very local SPH numerical ruptures due to a weak SPH method instability. These aspects are not very satisfactory, but this justifies the study on the cutting
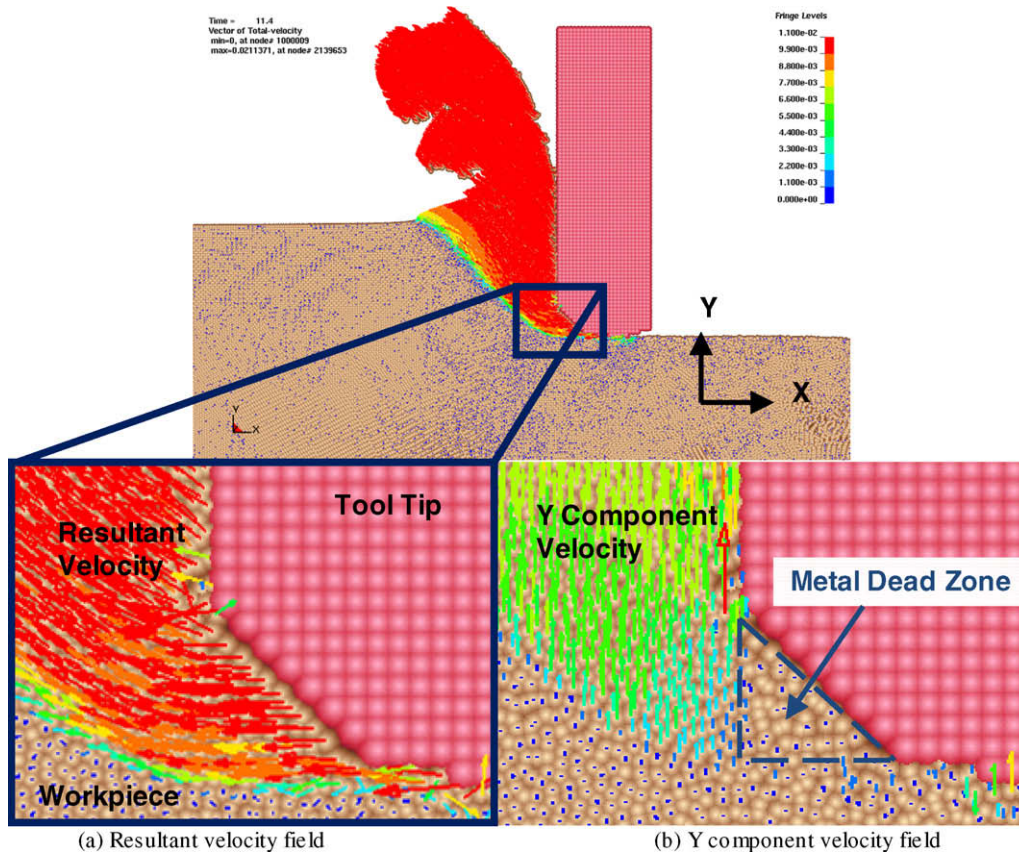

Fig. 10. Velocity field at tool tip: Metal dead zone identification.

Table 3
Johnson–Cook material model parameters for Ti6Al4V

References	A	B	n	C	m
[24]	862	331	0.34	0.012	0.8
[25]	1098	1092	0.93	0.014	1.1
[26]	783	498	0.22	0.028	1
[27]	859	640	0.22	0.000022	1.1

force variation and not on the force values. Nevertheless, this does not question the SPH model validity as a representative model of chip formation mechanisms with worn tool.

As mentioned in Section 4.2, the numerical/experimental mismatch on the chip morphology prediction should be explained by the assumption of an adiabatic process but also it can derive from the Johnson and Cook coefficients.

Experimental results, illustrated in Fig. 12, show a great influence of wear on the feed force (about 90%) and a less important increase of the cutting force (about 20%). The SPH cutting model results show a mean feed force increase of about 110% with tool wear and of the cutting force of about 10% (Fig. 12). So, the SPH model is able to correctly predict these variations, induced by the tool wear by only taking into account the tool tip shape changes.

4.4. Analysis

4.4.1. Experimentally identified wear mechanisms

At the beginning of the cutting process, the tool surface is considered as "perfect". During the first pass of machining, the temperature rise at the tool/chip interface will produce a titanium adhesion on the tool rake face. The titanium layer thickness increases during the machining process. At a given moment, because of excessive vibrations and/or shocks, this deposit will be extracted
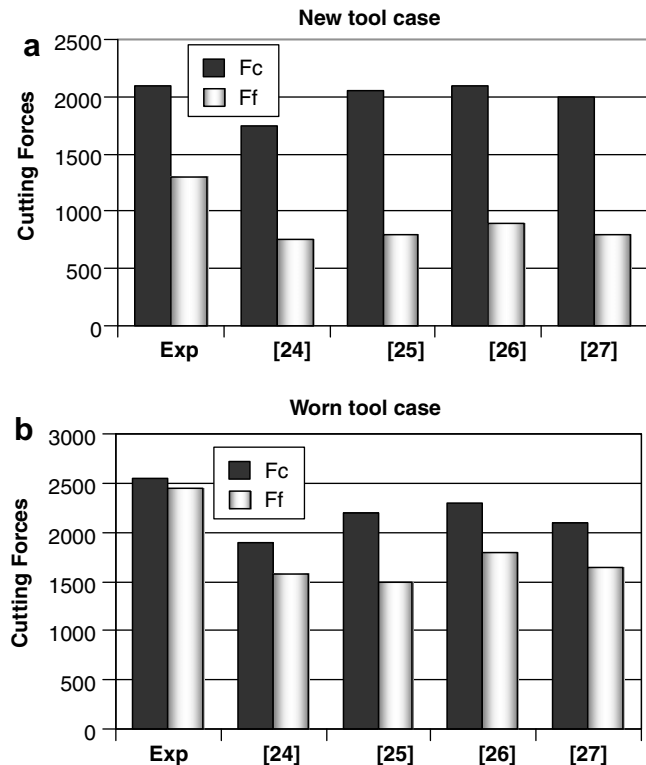

Fig. 12. Tool wear influence on cutting forces: SPH model compared to experimental data.

and carried away inside the chip. The high temperature generated by these extreme cutting conditions can also give rise to a diffusion phenomenon of the material tool binder (Co) inside the chip and also titanium towards the Co binder. This should weakened the chemical bonding between WC grains and Co with a subsequent evacuation of individual WC grains and titanium alloy microdeposits, as shown in Fig. 5.

This phenomenon produces the chipping of the tool edge in the area where the tungsten carbide grains have been carried away.

Fig. 11. Influence of material parameters on SPH predicted cutting forces and comparison with experimental cutting forces for (a) a new and (b) a worn tool.
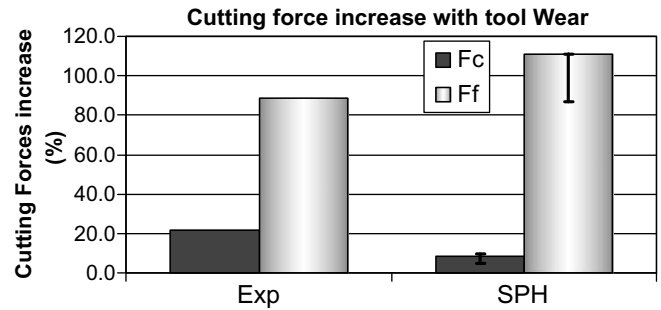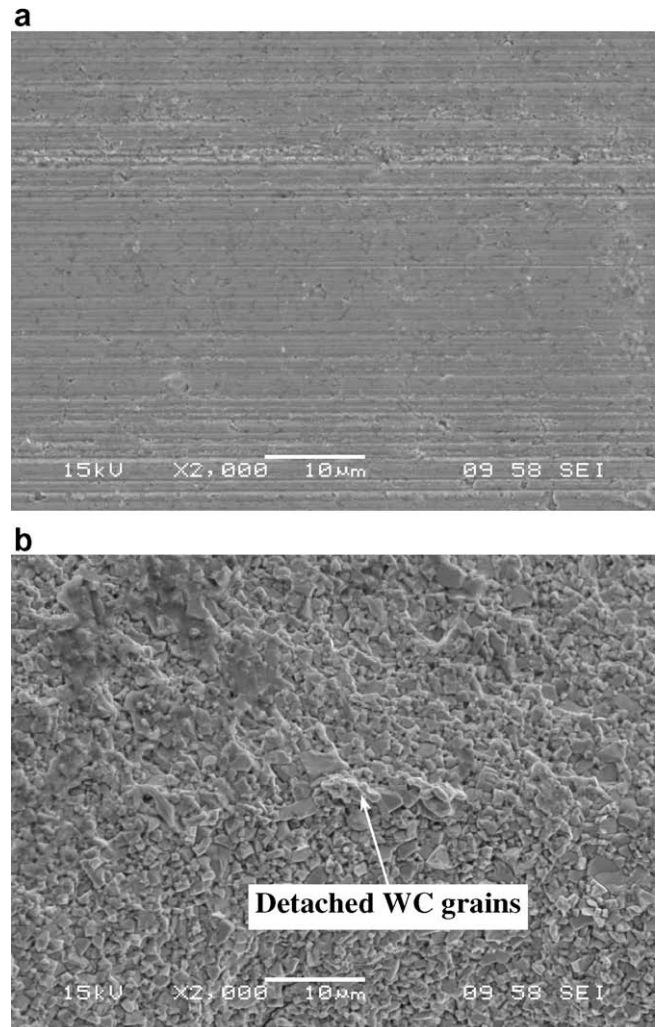

Fig. 13. (a) Area on the tool rake face before machining; (b) area on the worn tool rake face.

The state of the tool surface will change, becoming rougher. This will increase the titanium adhesion tendency and later on, the carbide grains extraction. All these phenomena will lead to an important change in the tool geometry. Fig. 13a and b show a detail of the tool surface in a new state and after machining.

4.4.2. SPH model contribution

SPH model correctly predicts the effect of wear modelled as a tool tip shape change on the cutting forces. This cutting force variation with wear is modelled without large variations of local friction. Indeed, a simplistic study of friction in SPH was implemented. The contact between two SPH surfaces representing, respectively, the tool and workpiece material is studied at various speeds. It allows to quantify friction through a Coulomb parameter calculation (Ft/Fn) obtained by SPH friction. This equivalent Coulomb friction evolution versus speed is given in Fig. 14. In fact, the friction coefficient evolves slowly with relative speed, i.e. a reduction of approximately 20%, between 0.01 m/s and 100 m/s.

Thus, the cutting force variation seems to be more affected by the geometrical edge modifications than by a significant local friction change. Indeed, Figs. 4 and 8 shows that the titanium deposit on the tool in the worn case looks like that in the new case.

Nevertheless, Fig. 2 shows an important change in chip rear face aspect. Typical experimental analysis concludes that this change is due to an important variation of contact type. The SPH model results suggest that the contact type change is not related to the tool/chip interface. Indeed, the metal dead zone formation illustrated in Fig. 10 creates a contact between the metal dead zone and the chip. This contact is thus established between two different zones but constituted of the same material (Ti6Al4V). The metal dead zone acts as a new tool tip and so a larger friction coefficient is developed at this "new tool tip". Finally, the tool/chip friction does not change but a new sticking zone is introduced by the metal dead zone formation. This important physical phenomenon explains the feed force increase with wear.

These precise details on the friction aspect are important because in classical cutting models, the coefficient of friction must be adjusted in order to correctly represent the cutting forces variation with wear [28] and thus the representation of a physical phenomenon (a metal dead zone formation [20]) is eclipsed. It would be interesting to implement modern techniques of high speed visioplasticity in order to check these numerical observations.

5. Conclusion

An experimental and a numerical approach were implemented in the framework of straight tungsten carbide tool wear analysis for the titanium alloy Ti6Al4V dry machining. Cutting and feed forces, chip morphology and w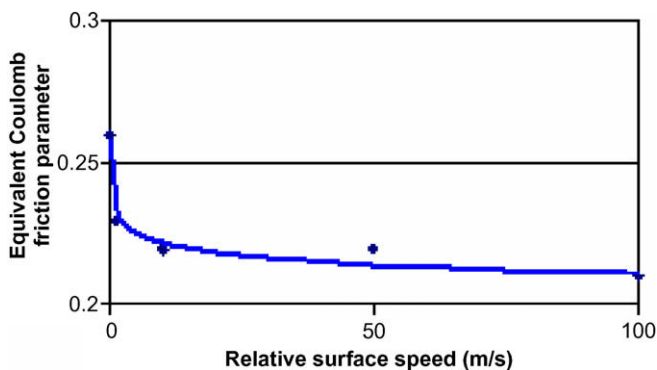ear evolution were studied. The experimental part made possible to identify characteristic evolutions. A particularly interesting aspect is that for large wear cases, the forces increase, and especially the feed force.

The analysis of the titanium deposits on the tool alone did not make possible to explain the important evolution of the feed force with tool wear by a tool/chip friction type change. Indeed, the titanium deposit only slightly evolves with the increase in wear. Thus, a numerical SPH 2D cutting model has been implemented as a helpful tool for understanding chip formation. This approach enables to explain the feed force increase with tool wear. Indeed, it showed that for raised wears, a metal dead zone appeared in front of the tool tip. This implies a large increase of the macroscopic apparent friction due to the contact between the Ti6Al4V dead zone and the Ti6Al4V chip. The local tool/chip contact is not changed between new and worn tool cases but the chip flow is different. Therefore, the SPH model supplements the experimental approach because it allows the identification of physical mechanisms hardly observable in experiments. Moreover, the meshless nature of the method makes possible to represent a dominating physical phenomenon in the chip formation with strongly worn tool: the metal dead zone.

Moreover, this study showed that a Coulomb type approach with classical FEM is not satisfactory in this case because it results in masking the metal dead zone formation. This numerical–experimental approach appears to be very interesting because it overcomes some limits of each method.

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Fig. 14. Coulomb friction evolution versus relative speed.

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