Adiabatic shear banding in high speed machining of Ti–6Al–4V: experiments and modeling

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Dedicated to Thomas Wright on his 65th birthday

Abstract

An experimental analysis of orthogonal cutting of a Ti–6Al–4V alloy is proposed. Cutting speeds are explored in a range from 0.01 to 73 m/s by using an universal high-speed testing machine and a ballistic set-up. The evolution of the cutting force in terms of the cutting speed and the development of adiabatic shear banding are analyzed. The shear band width and the distance between bands have been determined by micrographic observations. Their dependence upon the cutting velocity is analyzed. A modeling is proposed which restitutes well this velocity dependence. © 2002 Elsevier Science Ltd. All rights reserved.

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1. Introduction

Titanium alloys are regarded as extremely difficult to cut materials. The tool wear is intense because of the high cutting temperature due to low thermal conductivity. In addition the high chemical reactivity of titanium with the cut material produces a strong adhesion of the work-piece with the tool surface, Kahles et al. (1985), Le Maître (1986), Machado and Wallbank (1990). Measurements of tool wear and of cutting temperatures were considered among others by Komanduri (1982), Narutaki and Murakoshi (1983).
Numerous studies on machining of titanium alloys (analysis of chip formation and of cutting forces) have been carried out in the range of cutting velocities lower than 5 m/s, Komanduri and Von Turkovich (1981), Komanduri (1982), Narutaki and Murakoshi (1983), Larbi (1990), Bayoumi and Xie (1995), Diack (1995). In the work of Hoffmeister et al. (1999), larger velocities were considered in the range of 20 to 100 m/s.

Chip segmentation by shear localization is an important process which is observed in a certain range of cutting velocities. This phenomenon might be desirable in reducing the level of the cutting forces and by improving chip’s evacuation. The process of chip segmentation in titanium alloys was analyzed by Komanduri et al. (1981). Bayoumi and Xie (1995) have studied the effect of the cutting conditions on the formation of shear bands in Ti–6Al–4V. The analyses of segmentation frequency and of phase change within shear bands were carried out. On the theoretical point of view, Huang and Aifantis (1997) proposed a method for the prediction of shear band spacing during serrated chip formation. They have analyzed the effect of feed rate and cutting velocity on shear band spacing.

In this paper, an experimental study of localized shearing and chip serration is carried out for orthogonal cutting in a wide range of cutting velocities (0.01 m/s < V < 73 m/s). Large velocities were obtained with a ballistic set-up developed by Sutter et al. (1998); small velocities were performed on an universal high-speed testing machine.

The influence of the cutting velocity and of the depth of cut on the level of cutting forces and on the shear band morphology and spacing is analyzed. The theoretical results of Wright and Ockendon (1992) and of Dinzart and Molinari (1998), when transposed to the cutting process, predict that the band width varies as the inverse of the cutting velocity. This prediction is shown to be in agreement with the experimental observations. Other experiments made with a medium carbon steel are also found to match the theoretical predictions.

In addition the spacing of adiabatic shear bands is analyzed in terms of the cutting velocity, and a modeling is proposed which is based on the work of Wright and Ockendon (1996) and Molinari (1997).

2. Experimental set-up

To cover a wide range of cutting speeds, two different devices have been used. The low cutting speeds (from 0.01 to 1 m/s) are obtained with an universal high-speed testing machine. Two tools are symmetrically mounted on a tool’s holding fixture which is attached to the structure of the machine. The workpiece is attached on an actuator moving downwards to the tools with the cutting velocity $V$.

The second arrangement is constituted by an airgun set-up (Fig. 1). Projectiles are launched at different velocities ranging from 10 to 73 m/s. The workpiece is carried by the projectile which is guided in the launch tube and has enough kinetic energy to ensure a quasi stationary cutting speed. At the entry of the transmitter tube, two tools are symmetrically mounted on the same attachment as the one used on the universal high-speed testing machine (Fig. 1). After cutting, the projectile is stopped
by a shock absorber at the end of the transmitter tube. Orthogonal cutting is produced with a precision of less than 0.02 mm on the depth of cutting, thanks to the quality of manufacturing of the launch tube. The velocity of the projectile (impact velocity) is measured at the end of the launch tube with a set-up of three sources of light, fiber optics and photodiodes, and two time counters. The impact velocity is nearly equal to the initial cutting velocity. A correcting factor related to the impedance $A \rho C_0$ of the tube material should be actually used to convert the impact velocity into the cutting velocity:

\[
V_{\text{cutting}} = V_{\text{impact}} - \frac{F_0}{A \rho C_0}
\]

where $A$ is the cross-section area of the tube, $\rho$ and $C_0$ are, respectively, the mass density of the tube material and the longitudinal wave speed, $F_0$ is the transmitted
The present problem is of the order of 0.1 m/s when considering the maximum value of $F_0$ obtained during the tests. Therefore the correction related to the impedance of the Hopkinson tube is negligible for the values of the cutting speed considered here ($V > 7$ m/s).

The projectile speed is almost constant during the cutting process, the mass of the projectile being calculated so as to have a kinetic energy large with respect to the cutting energy. More details on the experimental set-up can be found in Sutter et al. (1998).

The longitudinal cutting force is calculated from two independent measurements with strain gages on the transmitter tube and the tool holding fixture. The specimen is rectangular (Fig. 2) (height $h = 44.4$ or 45 mm, cutting width $w = 10$ mm and length of cut $L = 12$ mm).

Note that this test technique has some similarities with experimental techniques used in material testing (see Campbell and Ferguson, 1970; Klepaczko, 1994).

The depth of cut is obtained precisely for each specimen by the measurement of the values $h_i$ and $h_c$ before and after the test respectively (see Fig. 2). The depth of cut (or uncut chip thickness) is given by:

$$t_1 = \frac{h_i - h_c}{2}$$

To avoid the fracture of the tools at high impact velocities, small depths of cut have been considered ($t_1 = 0.12$ and $t_1 = 0.25$ mm).

All the tests were carried out with carbide tools, a new tool being used for each shoot. The tools were square shaped without chip-breaker. The rake angle is $\alpha = 0$ (see Fig. 3 for the definition of $\alpha$).
3. Results and discussion

The workpiece material used in the experiments is a titanium alloy (Ti–6Al–4V), with chemical composition specified in Table 1. Under quasistatic conditions, the nominal yield stress and the maximum value of the nominal stress in a tensile test are: \( \sigma_y = 830 \) MPa (at 0.2% deformation); \( \sigma_{\text{m}} = 900 \) MPa. Orthogonal cutting is performed for various velocities in the range \( 0.01 \text{ m/s} \leq V \leq 73 \text{ m/s} \).

3.1. Cutting force

A typical oscillogram from the strain gages on the tool’s holding fixture is represented in Fig. 4 (for \( V = 12.6 \) m/s). The measurement device being calibrated with a

![Diagram](image_url)

Fig. 3. Orthogonal machining. Cutting conditions are specified by: \( \alpha \), rake angle; \( t_1 \), uncut chip thickness; \( V \), cutting velocity.

<table>
<thead>
<tr>
<th>Elements (%)</th>
<th>Ti</th>
<th>Al</th>
<th>V</th>
<th>C</th>
<th>Fe</th>
<th>O_2</th>
<th>N_2</th>
</tr>
</thead>
<tbody>
<tr>
<td>TA6V</td>
<td>Min. Base</td>
<td>5.50</td>
<td>3.50</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>Max.</td>
<td>6.75</td>
<td>4.50</td>
<td>0.08</td>
<td>0.30</td>
<td>0.20</td>
<td>0.05</td>
</tr>
</tbody>
</table>
direct impact test on the Hopkinson tube, the signals can be converted into values of
the longitudinal cutting force. Following a transient period of 100 μs, quasi-sta-
tionary cutting conditions are obtained (see Fig. 4). Oscillations on this diagram

![Oscillogram from strain gages on the tool’s holding fixture.](image1)

**Fig. 4.** Oscillogram from strain gages on the tool’s holding fixture.

![Graph of longitudinal cutting force $F_m$ as a function of cutting speed $V$.](image2)

**Fig. 5.** Longitudinal cutting force $F_m$ as a function of the cutting speed $V$. Results are compared with those of Hoffmeister et al. (1999) and Larbi (1990).
cannot be correlated to the frequency of adiabatic shear banding, but are related to wave reflections in the holding fixture.

The evolution of the cutting force $F$ (divided by $w t_1$), exerted on a single tool, in terms of the cutting velocity $V$ is reported in Fig. 5. The cutting conditions are: rake angle $0^\circ$, cutting width $w = 10$ mm, uncut chip thickness $t_1 \approx 0.12$ mm. A rapid decay of $F$ is observed in the range of velocities $0.01 \text{ m/s} \leq V \leq 10 \text{ m/s}$, followed by stabilization at the level $F \approx 1500 \text{ N}$. The decay of $50\%$ on the amplitude of the cutting force can be attributed mostly to the reduction of the friction coefficient along the tool-chip interface when the cutting speed is increased. Adiabatic shear banding during chip formation can possibly have an effect on the reduction of $F$.

Our results for Ti–6Al–4V are compared in Fig. 5 with those of Larbi (1990) and Hoffmeister et al. (1999). The trends are similar: for $V \geq 20 \text{ m/s}$, the cutting force is weakly dependent on $V$. The rapid decrease of $F$ at low cutting velocities is also observed in the experiments of Larbi (1990). No data are provided by Hoffmeister et al. (1999) at low cutting velocities. The large differences in the level of the cutting forces between the results of the different authors, may be attributed to variations in the cutting conditions (differences in the rake angle $\alpha$, in the type of tools...). The thermomechanical history of the workpiece materials may also be different.

3.2. Adiabatic shear banding

Chips have been collected and embedded into resin; the lateral section was polished before to be etched. Chip serration was observed to be related to adiabatic shear banding which is known to be easily triggered in titanium alloys.

Adiabatic shear bands are the manifestation of a thermomechanical instability resulting in the concentration of large shear deformations in narrow layers. This strain localization is accompanied by a large local growth of the temperature which is a necessary condition to have adiabatic shearing. In general, metals subjected to low strain rates, manifest no adiabatic shearing because heat diffusion tends to make the temperature uniform in the specimen. In titanium alloys, adiabatic shear banding is favored by the low value of the heat conductivity ($\approx 16 \text{ W/m K}$). For velocities lower than $1.2 \text{ m/s}$, chip serration is related to the development of deformed shear bands (see Fig. 6), which are the manifestation of the thermomechanical instability described above. However at these low values of the cutting velocity, the instability process is weak, and the localization is not as sharp as for high velocities.

For cutting velocities $V$ higher than $12 \text{ m/s}$, the adiabatic shear bands have marked boundaries (see Fig. 7). They seem to be transformed bands, in which phase transformation has occurred.

The shear band thickness $d$ was measured for different cutting velocities. Experimental points are shown in Fig. 8 for two depths of cut $t_1 = 0.12$ mm and $t_1 = 0.25$ mm. The variation of the shear band width in terms of the cutting velocity is given in a log–log diagram. The slope $-1$ is indicative of a physical law of the following form:

$$d \approx \frac{b(t_1)}{V}$$

(1)
which assumes that $d$ is inversely proportional to the cutting velocity $V$. The factor $b$ is dependent on the uncut chip thickness $t_1$. Similar results from Sutter et al. (1997) are shown in Fig. 9 for a medium carbon steel, and for two values of the depth of cut, $t_1 = 0.3$ mm and $t_1 = 0.5$ mm. Again, a good agreement is obtained with the law (1).
The law (1) has been demonstrated in a slightly different context by Wright and Ockendon (1992) and Dinzart and Molinari (1998). These authors have determined the width of an adiabatic band formed in a layer of finite thickness subjected to a shear deformation due to applied velocities $V^*$ on the boundaries (Fig. 10). The shear band thickness $d$ was obtained in terms of the applied velocity $V^*$ as:

Fig. 8. Adiabatic shear band width as function of the cutting speed for a titanium alloy Ti–6Al–4V. Two values of the uncut chip thickness are considered. The slope-1 in this log–log diagram is in agreement with the theoretical result of Eq. (1).

Fig. 9. Adiabatic shear band width as function of the cutting speed for a medium carbon steel. The measurements were made by Sutter et al. (1997) and are presented here in a log–log diagram. Two values of the uncut chip thickness are considered. The slope-1 is in agreement with the theoretical result of Eq. (1).

The law (1) has been demonstrated in a slightly different context by Wright and Ockendon (1992) and Dinzart and Molinari (1998). These authors have determined the width of an adiabatic band formed in a layer of finite thickness subjected to a shear deformation due to applied velocities $\pm V^*$ on the boundaries (Fig. 10). The shear band thickness $d$ was obtained in terms of the applied velocity $V^*$ as:
\[ d = 6\sqrt{2m} \frac{k_0\theta_0}{V^*a\tau_0} \]  

(2)

where \( m \) the strain-rate sensitivity, \( k_0 \) the heat conductivity, \( a \) the thermal softening parameter, \( \tau_0 \) the shear flow resistance, \( \theta_0 \) the initial temperature. The constitutive law used to demonstrate (2) was of the following forms:

\[ \tau = \tau_0 \left( 1 - a \frac{\theta}{\theta_0} \right) \left( \frac{\dot{\gamma}}{\dot{\gamma}_0} \right)^m \]  

(3)

where \( \tau \) is the shear stress, \( \dot{\gamma} \) the shear rate, \( \tau_0, \dot{\gamma}_0 \) are a reference stress and a reference strain rate, respectively. The reference temperature \( \theta_0 \) can be taken as the initial temperature. Note that a linear temperature softening is considered in (3). Strain hardening is not accounted for in the constitutive law (3), because it has a weak influence at the large deformations involved in the late stage of the shear band evolution.

It is worth to remark from (2) that the shear band width does not depend upon the thickness of the shear layer. This result implicitly assumes that the thickness of the shear layer is large enough with respect to the shear band width. In the relationship (2), it is found that the band width is inversely proportional to the velocity difference \( 2V^* \), between the two boundaries of the layer.

In high speed machining, the chip is mainly formed by shearing within the primary shear zone, which is a layer emanating from the tool tip with an angle \( \phi \) with respect to the direction of the cutting velocity \( V \) (Fig. 3). By \( V_{s0} \) and \( V_{s1} \), we designate the shear components of the cutting velocity \( V \) and of the chip velocity \( V_c \) along the direction \( x \) tangent to the primary shear zone (Fig. 3). The jump in the shear components of the velocity is given by:
\[
V_{\dot{\alpha}} - V_{\beta\dot{\alpha}} = V \frac{\cos \alpha}{\cos(\phi - \alpha)}
\] (4)

In general, the shear angle \(\phi\) depends on the cutting speed \(V\). However, at high cutting speeds, the dependence of \(\phi\) upon \(V\) is weak, see Moufki et al. (1998). Therefore, for a fixed value of the rake angle \(\alpha\), the velocity jump can be considered as proportional to \(V\), when high cutting speeds \(V\) are considered.

In the problem analyzed by Wright and Ockendon (1992) and Dinzart and Molinari (1998), the strain-rate distribution within the shear layer is uniform prior to the development of the thermomechanical instability which leads to adiabatic shear banding. In metal cutting, the strain rate distribution within the primary shear zone is non uniform even in absence of adiabatic shear band. However to simplify the analysis of shear band patterning, the strain rate is assumed uniform through the thickness of the primary shear zone (when considering the fundamental solution with no thermomechanical instability), and is taken to be equal to the average value \(\dot{\gamma} = \frac{V_{\dot{\alpha}} - V_{\beta\dot{\alpha}}}{2h}\). Following this assumption, the shear velocity \(v^*\) in Eq. (2) can be replaced by:

\[
v^* = \frac{V_{\dot{\alpha}} - V_{\beta\dot{\alpha}}}{2}
\] (5)

Thus, using (2), (4) and (5), the dependence (1) of the shear band width upon the cutting velocity \(V\) is found. It is worth to observe that these theoretical predictions are in good agreement with experimental results (Figs. 8 and 9). Note, however, that we have presently no interpretation for the effect of the uncut chip thickness \(t_1\) on the shear band width \(d\). The relationship (2) for \(d\) has been derived for shearing of a layer of finite thickness \(2h\) infinitely extended in the shear direction \(x\) (Fig. 10). In the modeling of chip formation, this layer is identified with the primary shear zone (Fig. 3), which has a finite extension from the tool tip to the workpiece surface. Therefore the real problem should be addressed in a two-dimensional framework, in which the effect of the uncut chip thickness \(t_1\) could be analyzed.

Implicitly, we have assumed in the foregoing model that the tools do not vibrate during the cutting process. Tool vibrations can be neglected here due to the high elastic stiffness of the tool’s holding fixture. Therefore, chip serration is thought to be the result of the genuine thermomechanical instability generated within the primary shear zone, with no interference with the elastic energy of the structure supporting the tools.

Note that, in a real metal cutting process, the machine stiffness might be not high enough to prevent tool vibration. In this case, chip serrations are the result of a complex interplay between tool vibrations and the thermomechanical instability process in the primary shear zone. This point will be discussed later.

3.3. Chip serration

In the range of cutting velocities \(0.01 \text{ m/s} < V < 21 \text{ m/s}\), the chip is serrated but remains continuous, the segments being attached to each other. For \(V > 21 \text{ m/s}\), the chip is discontinuous and fragmented in small pieces.
The frequency of serration \( f \) can be defined, as being the number of segments produced per unit time. This frequency was obtained for a continuous chip as follows:

\[
f = \frac{\text{Number of segments}}{\text{Time of cutting}}
\]

The time of cutting is estimated in two different ways. First, it is measured from oscillograms as in Fig. 4; secondly, it can be calculated by dividing the length \( L = 12 \ \text{mm} \) of the specimen (see Fig. 2) by the cutting speed \( V \).

This frequency is reported in Fig. 11a in terms of the cutting speed \( V \) in a log–log diagram. The experimental points are remarkably aligned on a straight line with slope 7/5 in four decades of cutting velocities. Presently, we have no theoretical explanation of these results for the whole range of cutting velocities considered. However, an interpretation is proposed for velocities \( V > 12 \ \text{m/s} \) where the most intense adiabatic shearing is observed.

Wright and Ockendon (1996) and Molinari (1997) have determined the characteristic distance \( L_c \) between adiabatic shear bands obtained in a shear test at high strain rates. For a constitutive law of the form (3), \( L_c \) is given by:

\[
L_c = 2\pi \left( \frac{m^2 k c (1 - a)^2 \theta^2_0}{1 + \frac{1}{m}} \beta^2 \left( \frac{\dot{\gamma}}{\tau_0 a^2} \right)^3 \right)^{1/4}
\]

where \( k \) is the heat conductivity, \( c \) the heat capacity per unit mass, \( \beta \) the Taylor–Quinney coefficient characterizing the part of plastic work transformed into heat. The stress \( \tau_0 \) is:

\[
\dot{\tau}_0 = \tau_0 (1 - a) \left( \frac{\dot{\gamma}}{\dot{\gamma}_0} \right)^m
\]

\[
\dot{\gamma} = \frac{V^*}{h}
\]

where \( \pm V^* \) are the tangential velocities applied at the boundaries of the sheared layer of thickness \( 2h \) (Fig. 10). Strain hardening, which is not included in (3) and (6), could be taken into account Molinari (1997). However, this material characteristic will not affect the dependence of \( L_c \) upon \( \dot{\gamma} \) as expressed in (6). Since we are mainly interested in this paper by the effect of the cutting velocity on shear band patterning, the introduction of strain hardening effects is of no importance.

By combining (4)–(8), it is shown that the characteristic distance between shear bands formed in the primary shear zone, depends upon the cutting velocity as:

\[
L_c = AV^{-\frac{1-m}{4}}
\]
In general, the strain-rate sensitivity $m$ is small, and one has the following approximation, which is used in the rest of the paper:

$$L_c \approx A V^{-\frac{3}{4}}$$  \hspace{1cm} (9b)

The frequency of segmentation $f$ can be identified here with the number of shear bands formed per unit time:

$$f = \frac{V_N}{L_c}$$  \hspace{1cm} (10)

where $V_N$ is the normal component of the cutting velocity to the primary shear zone:

$$V_N = V \sin \phi$$  \hspace{1cm} (11)

By combining (9), (10) and (11), we have:

$$f = \frac{\sin \phi}{A} V^{7/4}$$  \hspace{1cm} (12)

With the assumption that $\phi$ and $h$ are weakly dependent on $V$ for the high cutting velocities considered here, the relationship (12) gives the slope $7/4$ in Fig. 11a, in agreement with the experimental results for adiabatic shearing (see the enlarged box).

It must be emphasized that the theoretical result (6) was obtained for a shear layer having a thickness larger than the distance between adiabatic shear bands, $L_c$. In high speed machining, the primary shear zone can be very thin (some ten microns). Typical values of $L_c$ corresponding to $V > 12$ m/s are of the order of 30–40 μm. Therefore, we are at the limit of validity of the theory used to obtain the result (6). However, the prediction seems to be in agreement with the observations.

When chip segmentation is due to adiabatic shearing, the serration frequency is governed by the relationship (12), which seems to be an universal law in terms of the velocity dependence, the exponent $7/4$ being not dependent on the material and the cutting conditions considered. To check this point, a different material is now considered. Sutter et al. (1997) have analyzed the chip segmentation of a medium carbon steel due to adiabatic shearing for cutting velocities $18$ m/s ≤ $V$ ≤ $62$ m/s. Reporting for $\alpha = 0$, these results in the log–log diagram of Fig. 11b, the same slope $7/4$ is found as in Fig. 11a, in agreement with the law (12).

The distance between adiabatic shear bands has been measured. For the discontinuous chips obtained at velocities $V > 30$ m/s, this distance was evaluated by using chip pieces in which a series of small segments were still attached to each other. However, the frequency of serration could not be measured for these velocities, because the chip was broken in many pieces and could not be reconstructed entirely. This explains why experimental data for $V > 30$ m/s are missing in Fig. 11a. The slope $-3/4$ in the log–log diagram of Fig. 12 corresponding to the theoretical prediction (9), is in agreement with experimental data.
In their work on orthogonal cutting of Ti–6Al–4V and AISI 304 stainless steel, Bayoumi and Xie (1995) are questioning the correlation between chip segmentation and dynamic instabilities such as chatter. When their results on chip serration frequency are presented in terms of the cutting velocity in a diagram similar to Fig. 11,

![Graph showing frequency of chip segmentation as a function of cutting speed for Ti–6Al–4V and medium carbon steel.](image)

**Fig. 11.** Frequency of chip segmentation $f$ as a function of the cutting speed $V$ for a rake angle $\alpha = 0$. Two materials are considered: (a) Ti–6Al–4V: the experimental results in the enlarged box correspond to cutting velocities for which transformed adiabatic shear bands are obtained ($V \geq 12$ m/s). The slope 7/4 is predicted theoretically by Eq. (12); (b) medium carbon steel.
a slope approximately equal to $13/10$ is found. As in our work, the value of the slope is identical for the two materials considered and does not depend either on $t_1$. The difference with our result (slope $7/5$ in Fig. 11a) is not large. The elastic compliance of the lathe used to get orthogonal machining in the experiments of Bayoumi and Xie (1995), may have an effect on the serration frequency. In our set-up, the tools are attached on a rigid support; therefore, our results are weakly affected by the elasticity of the system.

4. Conclusions

Chip serration has been analyzed for orthogonal cutting of Ti–6Al–4V in the range of cutting velocities $0.01 \text{ m/s} \leq V \leq 73 \text{ m/s}$. Adiabatic transformed shear bands were observed for ballistic test at velocities $12 \text{ m/s} \leq V \leq 36 \text{ m/s}$. Chip serration at lower velocities is due to a weaker thermomechanical instability leading to deformed adiabatic shear bands, the localization of plastic flow being not so intense as for large cutting velocities.

The patterning of adiabatic shear bands has been identified experimentally, by measurements of the shear band width and of the separation distance between bands (chip segment width). This patterning is shown to be strongly dependent upon the cutting velocity $V$. The width of transformed adiabatic shear bands ($V \geq 12 \text{ m/s}$) decreases with the cutting velocity as $V^{-1}$. The separation distance decreases approximately as $V^{-3/4}$ for $V \geq 12 \text{ m/s}$. A theoretical modeling of these findings is proposed, based on results on adiabatic shear banding obtained in a different context by Wright and Ockendon (1992) and Dinzart and Molinari (1998) for the shear band width, and Wright and Ockendon (1996) and Molinari (1997) for the separation distance. These results are transposed to high speed machining by assuming that adiabatic shear banding is generated in the primary shear zone where the material suffers intense shear rates. They are shown to well predict the dependence of the adiabatic shear band patterning upon the cutting velocity.
We have not developed here a modeling of chip serration for the entire range of cutting velocities. However, the dependence of the serration frequency $f$ upon the cutting velocities, $f_{\text{experimental}} \approx V^{7/2}$, is close to the law predicted theoretically by assuming that chip serrations are due to a thermomechanical instability ($f_{\text{theoretical}} \approx V^{7/4}$). These laws do not apply to chip fragmentation by brittle rupture.

Note that the scaling laws describing the velocity dependence of the shear band patterning seem to be universal in the sense that the velocity exponent does not depend on the material and the cutting conditions considered. It is important to remember that these results are restricted to cutting conditions with no chatter (rigid support of the tools), and to problems where chip serration is generated by a thermomechanical instability.

References


