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Practical applications of the “energy–triaxiality” state relationship in metal cutting

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ABSTRACT
Most of the energy spent on metal cutting is due to the unavoidable plastic deformation of the layer being removed during its transformation into the chip. Based on the new principle of metal cutting being a purposeful fracture process, the dominant parameter that controls this process in orthogonal metal cutting (OMC) is the triaxiality state. Therefore, the chip triaxiality state in the deformation zone can be correlated to the energy of the unwanted plastic deformation for a particular cutting configuration. This article investigates this type of correlation by changing the cutting tool geometry. A series of finite element (FE) simulations were performed for various tool rake angles showing a strong relationship between the stress triaxiality state parameter in the deformation zone and the required cutting force components.

KEYWORDS
Damage; fracture; material constitutive; metal cutting; plane strain; rake angle; steel 1045; triaxiality

Introduction
Metal cutting is a complex process where a layer of the work material undergoes large deformation under a compound state of stress before separation into a chip. The physics behind many practical phenomena occurred in metal cutting is still unexplained due to the complexity of the topic. The researchers in metal cutting field have different views on the fundamental interpretation of the cutting process. Among these discrepancies, there is the historic debate on the presence of crack in metal cutting. The difference between handling metal cutting as a deformation versus fracture fundamentally changes the interpretations and explanations even for the most common observations. Unlike the continuous deformation process, fracture is sensitive to parameters related to the stress state such as triaxiality state parameter.

It was observed for a long time that the rake angle has a great influence on the cutting process. As a result, this angle has drawn great attention from the researchers and professionals. Shaw and Booster (1988) argued that the specific cutting energy...
(and thus the cutting force) decreases about 1% per degree increase in the rake angle, while Dahlman et al. (2004) showed that by controlling the rake angle, it is possible to generate tailor-made machining residual stresses in the product. Gunay et al. (2005) in their experimental study found that a change in the rake angle from 0° to +2.5° resulted in a 2% reduction of the cutting force, while a change from −2.5° to 0° resulted in a 3.4% reduction. Tetsuji et al. (1999) in their tests on rock cutting found that the cutting force of the bit with a +20° rake angle decreased about 30–80% (depending upon other machining parameters), compared to that of the bit with a −20° rake angle. Moreover, an increase in cutting force with the cutting depth becomes lower with an increase in the rake angle. Gunay et al. (2005) performed a detailed experimental study of the influence of the rake angle in machining of AISI 1040 steel. They found a very small influence which diminishes at higher cutting speeds. Saglam et al. (2007) performed an extensive research program on machining of AISI 1040 steel bars hardened to HRC 40 to reveal the effect of tool geometry. It was also found that the influence of the rake angle depends on the tool cutting edge angle. More dramatic influences of the rake angle on the cutting force and temperature were found for high cutting speeds. A complete analysis of the influence of the rake angle on the cutting process as well as recommendation for the selection of the so-called effective rake angle was presented by Astakhov (2010). He argues that multiple experimental findings point out that the rake angle directly correlated on the amount of work of plastic deformation in metal cutting defined by the chip compression ratio.

Our analysis of the known facts on the rake angle significance in metal cutting suggests that a detailed explanation on the influence of the rake angle in metal cutting has become not only of scientific but also of great practical interest and significance in the cutting tool design and applications.

Based on the notion that metal cutting is a purposeful fracture of the work material (Astakhov, 2006), the current article aims to provide a physics-based explanation of the influence of the rake angle. This is accomplished by showing that the energy spent on the cutting process can be correlated to the stress triaxiality state in the primary deformation zone, due to the fact that the stress-state parameter affects the ductility of the work material and thus the energy required for chip separation.

**Known facts and unexplained phenomena**

The rake angle \( \gamma \) comes in three varieties, positive, zero (sometimes is referred to as neutral), and negative as shown in Figure 1a–c, respectively. There is a great body of experimental and numerical modeling results dealing with the influence of the value and sign of the rake angle on the machining process. The role and importance of the rake angle in metal cutting is not well understood because the available data are contradictory and often misleading. Moreover, the available studies did not take a system approach in the consideration of the influence of the rake angle on various outcomes of the cutting process. Rather, one outcome parameter is considered at
a time, for example, the cutting force only, while others, for example, tool life, are ignored. Using these data, a practical tool/process designer cannot make an intelligent selection of the proper rake angle for a given application (Astakhov, 2010).

It is a common belief among the specialists in the field that a sharper cutting tool requires less cutting energy. However, no physical explanation is offered in how the material behaves when the rake angle changes and what causes such enhanced performance. Furthermore, no explanation is provided for the fact that the tool life decreases with the increasing rake angle because the reduced energy consumption (and thus lower cutting force) actually should lead to increased tool life.

The problem with explanation of the influence of the rake angle and other parameters of the tool geometry can easily be resolved, and thus selection of these parameters together with the parameters of the machining regime (e.g., the feed and depth of cut) can be optimized if the definition of the metal cutting process as the purposeful fracture of the work material is used. As the work of plastic deformation to fracture depends on the state of stress triaxiality in the deformation zone, it can be suggested that the variation of the stress triaxiality (and thus the energy required by the cutting system) causes the reported influence of the rake angle. This article aims to show that this is the case in metal cutting.

**Underlying principles of the article**

There are two basic underlying principles in the current article: (1) the system definition of metal cutting and (2) the deformation law.

**System definition of metal cutting**

Based upon the observations, findings, and lessons learned from the history of metal cutting research (such as the conclusions from Time, 1870; Time, 1877; Tresca, 1864, 1873; Reuleaux, 1900; Taylor, 1907; and other pioneer engineers), Astakhov and Shvets (1998) formulated the system concept in metal cutting. According to
this concept, the process of metal cutting is defined as a forming process, which takes place in the components of the cutting system that are so arranged that the external energy applied to the cutting system causes the purposeful fracture of the layer being removed. This fracture occurs due to the combined stress, including the continuously changing bending stress causing a cyclical nature of this process. The most important property in metal cutting studies is the system time. The system time was introduced as a new variable in the analysis of the metal cutting system, and it was conclusively proven that the relevant properties of the cutting system’s components are time dependent (Astakhov and Shvets, 1998).

**Deformation law**

It has been revealed that the energy of plastic deformation of the layer being removed in its transformation into the chip is the greatest in machining of ductile materials, e.g., steels (Astakhov and Xiao, 2008). The greater the energy of plastic deformation, the lower the tool life, the quality of the machined surface, and the process efficiency. Therefore, the prime objective of the cutting process design is to reduce this energy to its possible minimum by the proper selection of the tool geometry, tool material, machining regime, metal working fluid (MWF), and other design and process parameters (Astakhov, 2010).

**Development of the computational model**

To simulate the cutting of ductile metals, a material constitutive model and the procedure to obtain parameters in this model as well as the analysis pertaining to the model validation have been developed and presented in earlier publication (Abushawashi et al., 2013). This section briefly discusses the development of workpiece material parameters, chip–tool interface, and validation of the computational model.

**Material modeling for the workpiece**

The model considers the material damage initiation, damage evolution, and final fracture. The parameters related to the deformation and fracture of steel AISI 1045 were obtained using a special double-notched specimen designed with a tuneable state of stress triaxiality. The model was implemented as a user material subroutine in an explicit FEA code and used to simulate the orthogonal cutting process of steel AISI 1045.

The Johnson–Cook (JC) plasticity model [Equation (1)] was used for modeling the workpiece material. The model is suitable for metal cutting analysis because it includes the effect of the strain rate and temperature. The role of temperature and strain rate in finite element method (FEM) in metal cutting is considered in great detail by Astakhov (2011). The JC model parameters were optimized using least
Table 1. JC material model parameters for steel AISI 1045 obtained from the double-notched testpiece and compared with other references.

<table>
<thead>
<tr>
<th>Author / experiment</th>
<th>Initial yield A (MPa)</th>
<th>Hardening modulus B (MPa)</th>
<th>Strain hardening, n</th>
<th>Strain rate sensitivity, C</th>
<th>Strain softening, m</th>
</tr>
</thead>
<tbody>
<tr>
<td>Borkovec</td>
<td>375.0</td>
<td>552.0</td>
<td>0.4570</td>
<td>0.020</td>
<td>1.400</td>
</tr>
<tr>
<td>Forejt</td>
<td>375.0</td>
<td>580.0</td>
<td>0.5000</td>
<td>0.020</td>
<td>1.040</td>
</tr>
<tr>
<td>Jaspers</td>
<td>553.1</td>
<td>600.8</td>
<td>0.2340</td>
<td>0.034</td>
<td>1.000</td>
</tr>
<tr>
<td>Ozel</td>
<td>451.6</td>
<td>819.5</td>
<td>0.1730</td>
<td>9.00E-07</td>
<td>1.095</td>
</tr>
<tr>
<td>Based on Bai's exp.</td>
<td>553.1</td>
<td>309.9</td>
<td>0.1952</td>
<td>0.0134</td>
<td>1.000</td>
</tr>
<tr>
<td>Double-notched testpiece</td>
<td>333.0</td>
<td>538.9</td>
<td>0.1299</td>
<td>0.0134</td>
<td>1.000</td>
</tr>
</tbody>
</table>

Based on Jaspers’s results (Jaspers and Dautzenberg, 2002).

\[
\bar{\sigma} = \left[ A + B\bar{\varepsilon}^n \right] \left[ 1 + C\ln \left( \frac{\dot{\bar{\varepsilon}}}{\dot{\bar{\varepsilon}}_o} \right) \right] \left[ 1 - \left( \frac{T - T_o}{T_m - T_o} \right)^m \right] 
\]  

Although Table 1 shows the JC parameters for the same steel AISI 1045 (from different sources), they differ considerably. The differences can be partially explained that these parameters may not be for the same metallurgical state of this steel.

Figure 2 shows the material fracture locus obtained from the double-notched specimen experiment and the fitted curves using the Rice and the reduced JC damage models.

The fracture strain \( \bar{\varepsilon}_f \) scalar values were calculated from the DIC measurements for a number of specimens used in the material characterization experiment. As expected, the overall trend is observed, i.e., the amount of the material plasticity is proportional to the stress triaxiality state.

**Tool–chip interface friction model**

Due to severe normal stress at the tool–chip interface, the conventional proportional friction theory, the so-called Coulomb friction model, may result in shear
traction that exceeds the chip ultimate shear strength \( (A_{\text{stakhov}}, 2006) \). This usually occurs within the tool–chip contact length \( (l_c) \) near the cutting edge where the normal stress is high. To overcome this violation, a sticking–sliding contact model is usually implemented. The model limits the maximum shear stress to a prescribed value over the so-called plastic zone of the tool–chip interface \( (A_{\text{stakhov}}, 2006) \). To identify a friction model that is valid for metal cutting simulations, Rech et al. \( (2009) \) performed the pin-on-ring system analysis and used the tribometer to extract experimental data such as sliding velocity and pressure. The study was conducted for annealed steel AISI 1045 with TiN-coated carbide tools. It was assumed that the total friction coefficient is due to the effect of two phenomena: ploughing and adhesion. It is understood that for metal cutting simulations, only the adhesion type of friction is to be considered.

To isolate the ploughing portion of the friction so that only the friction due to adhesion can be quantified for cutting applications, a thermomechanical numerical analysis was conducted by Rech et al. \( (2009) \) to estimate the two quantities separately. The analysis was based on the comparison of the friction and heat flux with the experimental data obtained under similar conditions. The final static adhesion friction coefficient was found to be \( \mu_{\text{adh}} = 0.498 \). The final dynamic adhesive friction coefficient \( (\mu_{\text{adh}}) \) model which depends on the sliding velocity \( (V_{ls}) \) was governed by the following linear relationship:

\[
\mu_{\text{adh}} = 0.498 - 0.002 \cdot V_{ls}
\]  

The sliding velocity \( (V_{ls}) \) range used in the experiment was between 50 and 103 m/min. According to Equation (2), the higher the sliding velocity, the lower the adhesive friction coefficient. The above relation can be used in the FEM model to estimate the friction forces on the tool face. Depending on the cutting speed, different friction values may be used. The sliding velocity \( (V_{ls}) \) in Rech’s experiment is the same as the sliding velocity of the chip on the tool rake face. Therefore, the chip velocity can be used as the equivalent of the sliding velocity \( (V_{ls} = \nu_2 = \nu_1 / \xi) \) in Equation (2). However, since chip compression ratio (CCR) or \( \xi \) is an output cutting parameter, instead of hard coding the tool–chip interface model in FEM, it is possible to automatically estimate the actual sliding velocity in a real-time simulation and adjust the value of the adhesive friction coefficient \( (\mu_{\text{adh}}) \) accordingly. This eliminates the need for the trial-and-error iterations to find the proper value \( \mu_{\text{adh}} \).

For more details on the approach, material model, and the deformation parameters used in the current analysis, the reader is advised to review the published article \( (A_{\text{bushawashi et al.}}, 2013) \). Table 2 shows the test matrix under which the FE analyses were conducted.

### Model validation

The validation of the developed FEM model was performed using guideline provided by Astakhov \( (2011) \).
Table 2. Test matrix and the cutting conditions used in the investigate triaxiality state effect on the cutting process energy.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cutting speed, ( V ) (m/min)</td>
<td>120.0</td>
</tr>
<tr>
<td>Uncut chip thickness, ( t_1 ) (mm)</td>
<td>0.042</td>
</tr>
<tr>
<td>Depth of cut, ( d_w ) (mm)</td>
<td>4.0</td>
</tr>
<tr>
<td>Rack angle, ( \gamma ) (°)</td>
<td>VAR(^a) [0–40]</td>
</tr>
<tr>
<td>Clearance angle, ( \alpha ) (°)</td>
<td>7.0</td>
</tr>
<tr>
<td>Tool–chip friction</td>
<td>0.498</td>
</tr>
<tr>
<td>Tool edge roundness</td>
<td>Sharp</td>
</tr>
<tr>
<td>Tool material</td>
<td>TiN-coated carbide</td>
</tr>
<tr>
<td>Workpiece material</td>
<td>Steel AISI 1045</td>
</tr>
</tbody>
</table>

\(^a\) Test variable.

**Chip morphology**

To investigate the geometrical and metallographical similarities between simulations and experiments, the deformed shapes of the chips were compared. Figures 3 and 4 show these comparisons for two cutting experiments. The chip geometry and its flow characteristics were predicted for each case and their thicknesses were measured as shown in Table 3.

The shear bands, which can be seen from the microstructure of the experimental samples, were predicted by the conducted analysis in both cases.

To reveal the validity of the simulations, the uncut chip thicknesses were measured from experiments and determined from simulations and the CCRs were calculated based on these measurements. The CCRs of the chip samples from the two cutting experiments and simulations are shown in Table 3. The test/model values of the CCR are found to be close and the maximum error is 6.7%.

**Cutting forces**

Figures 5 and 6 present the cutting force vs. tool travel distance curves for Chip-A and Chip-B, respectively. The experimentally obtained cutting force was compared
with the cutting force obtained from FE simulations. The cutting force depends on the cutting process parameters such as the tool geometry, depth of cut [which is the width of cut in orthogonal metal cutting (OMC)], cutting speed and chip–tool friction. Although all these parameters were not changed during this experiment, any change in the outcomes is, therefore, due to the thickness of the layer being removed

**Table 3.** Characteristics of chip morphology and cutting forces (Fc).

<table>
<thead>
<tr>
<th>Chip#</th>
<th>(t_1) (mm)</th>
<th>(t_2) (mm)</th>
<th>CCR</th>
<th>(F_c) (kN)</th>
<th>(t_2) (mm)</th>
<th>CCR</th>
<th>(F_c) (kN)</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>0.042</td>
<td>0.151</td>
<td>3.556</td>
<td>0.475</td>
<td>0.161</td>
<td>3.796</td>
<td>0.438</td>
</tr>
<tr>
<td>B</td>
<td>0.025</td>
<td>0.104</td>
<td>4.074</td>
<td>0.279</td>
<td>0.098</td>
<td>3.851</td>
<td>0.272</td>
</tr>
</tbody>
</table>

**Figure 4.** Chip-B FE prediction vs. experiment—chip morphology.

**Figure 6.** Chip-B FE prediction vs. experiment—cutting forces.
The cutting forces in the two different cases are well predicted by the FE simulations. Due to the compliance of the testing system, there was a considerable amount of change in the cutting force and chip thickness, particularly in the Chip-B case. Therefore, the most recent cut, i.e., the later segment of the experiment was selected as the window of interest for comparison with simulations, as shown in Figures 5 and 6. The uncut chip thickness $t_1$ and chip thickness $t_2$ were measured over this period. The average values of the cutting force were calculated and summarized in Table 3.

**Results and discussion**

To investigate how the rake angle affects the triaxiality state and the energy required for cutting, a number of FE simulations of OMC of steel AISI 1045 were performed. The cutting conditions were all similar except for the tool rake angle. The rake angle in this experiment was varied in the range from $0^\circ$ to $40^\circ$. As shown in Figure 7, the triaxiality state values in the primary deformation zone increase significantly when the rake angle is reduced. This implies an increased material ductility for lower rake angles and consequently larger deformation of the work material in its transformation into the chip. In addition, Figure 7 shows that the chip thickness, and thus CCR, reduces when rake angle increases as observed in practice. Obviously, the simulation shows that the state of triaxiality increases with the rake angle at the deformation zone, and more importantly near the point of chip separation from the rest of the workpiece which lowers the strain at fracture of the work material.

The chip structure also changes dramatically as rake angle increased. The chip structure shown in Figure 8 becomes smoother, uniform, and it undergoes much less plastic deformation as predicted.

As mentioned above, a possible decrease of tool life with the increasing rake angle observed in some practical applications should be explained. Such an explanation directly follows from Figure 8, which shows that the length of the tool–chip interface reduces with the increasing rake angle. It is also followed from the Poletica criterion
Figure 7. FE simulations of chip formation showing triaxiality state contours obtained from cutting tools with different rake angles.

[defined as the ratio of the contact length \((l_c)\) to the uncut chip thickness \((t_1)\)] as discussed by Astakhov (2006). If the rate of the contact stress decreases in higher rake angles due to the reduction of the normal stress and is higher than the effect of a lower contact length leading to an increase in the contact stress, the tool life may be improved. Such a phenomenon was first noticed in the study of cutting tools with the so-called restricted (or limited) length of the tool/chip interface as studied by

Figure 8. FE simulations of chip formation showing chip structures obtained from cutting tools with different rake angles.
Takeyama and Usui (1958), Chao and Trigger (1959), Hoshi and Usui (1962), and Usui et al. (1964).

As discussed above, the amount of chip plastic deformation decreases when tool rake angle increased (Figure 9) because the loading conditions caused by the increased rake angle elevates the state of stress triaxiality and lowers the fracture strain at the separation zone near the tool tip. Therefore, the cutting energy/forces are expected to be lower accordingly. Note that the plastic strain contours shown in Figure 9 represent the accumulated value that includes the postdamage strains.

Figure 10 confirms the fact that the lower the rake angle, the higher the cutting forces required to overcome the material resistance. The simulation suggests that rake angle increase can reduce the energy spent on the cutting significantly (by up to 59%) in the considered range. A summary table that contains the average cutting forces, average radial forces, as well as chip thickness and CCR is shown in Figure 7.
Although the radial force \( F_p \) defined as the force acting perpendicular to the direction of the primary motion (the cutting speed) is considered as having no contribution to the cutting power (energy) as the tool does not move in this direction, it provides important information on:

- FEM proper assessment of the radial force and thus adds to the validity of the whole model.
- Extent of the “negative” radial force that may cause tool chatter in real machining.

It was noticed in cutting soft work materials, such as brass, copper, Babbitt, using a tool with a high rake angle, the tool jumped ahead of the feed into the workpiece causing vibration, often referred to as chatter in machining. To understand why it happens, consider a simplified force model for machining with a tool having a high rake angle as shown in Figure 11. When the tool works, the radial component \( F_p \) of the resultant force \( R \) normally pushes the tool out of the workpiece. However, it may not be the case in machining with a tool having a high rake angle. As follows from the model shown in Figure 11, the radial force is calculated as:

\[
F_p = F_f \cos \gamma - R \sin \gamma + F_q
\]  

where \( F_f \) is the friction force over the tool–chip interface and \( F_q \) is the force on the tool flank that depends on the flank angle, tool wear, MWF, and other cutting parameters (Astakhov, 2006). This force can be accounted fairly well when its specific value of 30–60N/1 mm of the cutting edge length is considered (Astakhov, 2010).

The first component \( (F_f \cos \gamma) \), which pushes the tool away from the workpiece, decreases with the rake angle, while the second component \( (R \sin \gamma) \), which pulls the
Figure 12. Chatter marks on the machined surface.

Therefore, as the rake angle increases and a sharp cutting tool is used (small $F_q$), the radial force $F_p$ can be directed into workpiece, which is the root cause of the described phenomenon (chatter). Its typical appearance is shown in Figure 12.

Therefore, if FEM is constructed properly, the computational results should reveal the presence of the “negative” radial force (validity of the model) and its extent (to be used in chatter prevention calculations) (Davim and Astakhov, 2011).

Figure 13 shows the predicted radial forces for the different tool rake angles. As expected, $F_p$ is proportional to the rake angle, the lower the angle, the higher the forces. What is more important, however, is the “negative” radial force for high rake angles.

Figure 13. FE predictions of the radial forces obtained from cutting tools with different rake angles.
angles, which was predicted using a simple model of the normal stress over the tool (Astakhov, 2010). The simulations support these notions and suggest that the point of radial force balance in this experiment occurs at about 28° rake angle.

**Practical considerations**

**Strength of the cutting tool wedge**

Reading the previous section, one might argue, however, that a high positive rake angle is not very practical as the cutting tool wedge (the part of the tool material between the rake and the flank faces of the tool) becomes so weak that it can break easily if some fluctuations of the cutting force occur. Such fluctuations traditionally occur due to tool/workpiece runout, misalignments in the machining system, lack of structural rigidity in this system, etc. It is instructive to explain that although the listed factors can be significant, the whole described notion of tool fracture is a bit outdated.

In the not-too-distant past, the components of machining system were far from perfect in terms of assuring normal tool performance. Under these conditions, the use of cutting tools with high rake angles was impossible particularly if such a tool was made of a “brittle” (for such conditions) tool material as, for example, a sintered carbide. Adjusting to these conditions, tool researchers and manufacturers developed “forgiving” carbide tools made of high-cobalt carbide grades and with negative rake angles. The price to pay included a low tool life and limited cutting speed and feed (productivity). For many years, a stable, though fragile, balance between inferior design/geometry cutting tools and poor machining system characteristics was maintained.

This has been rapidly changing since the beginning of the 21st century. Modern submicrograin carbides possess sufficient fracture toughness. For many years, polycrystalline diamond (PCD)-brazed and indexable cutting inserts were used for this purpose with negative rake angles to cover up for imperfect machining systems. Due to recent development of ultramicrograin PCDs, advanced cutting tool manufacturers began to offer PCD insert with high positive (up to 10°) rake angles which have significantly improved high-speed machining of high-silicon aluminum alloys widely used in the automotive industry in terms of tool life, machined surface integrity, reduced cutting forces, etc. Unfortunately, the available recommendations for the suitable tool geometries did not reflect great advances made in the last 5–10 years in the properties of tool materials and coatings.

Gradually, some tool manufacturers began to offer tool with extremely high rake angles primarily for machining of aluminum alloys and copper. For example, Robertson Precision, Inc. (Redwood City, CA) developed Shear Geometry® cutting tools with extremely high rake angles. Figure 14 shows an example of such tools and the chip formed in machining of an aluminum alloy. The success of this tool became possible with the development of a special submicrograin sinter–hot isostatic pressed carbide tool material.
Nowadays, milling tools with high rake angles have become common. For example, Big Kaiser Precision Tooling Inc. (Elk Grove Village, IL) offers full cut mill FCM type with 20° rake angle. Allied Machine & Engineering Corporation (Dover, OH) offers high rake geometry on its drills which is specifically designed to improve chip formation in materials with very high elasticity, extremely poor chip forming characteristics, and low material hardness. Leading tool manufacturers also offer high rake CCGT inserts (Figure 15) intended for non-ferrous materials instead of CCMT inserts. Practical machinists found soon that such inserts can cut practically anything. Although regular CCMT inserts often have some positive rake angle, CCGT inserts offer much higher rake angles. The major insert manufacturers have
special lines of this style insert: ISCAR CCGT–AS, Kennametal CCGT–HP, Valenite CCGT–1L, Seco CCGT21.51F–ALKX, etc. Each one has a slightly different sales pitch about why one should use the insert. ISCAR is pushing them as offering such a fine finish for aluminum that no grinding is needed, for example. The recommended materials even vary across the lines. What started out as aluminum super finishing insert, then extended in applications to high-temperature alloys, stainless, and other possibilities.

**Chip breakability**

When using cutting tools with high rake angles, one needs to keep in mind the chip handling problem (Astakhov, 2010). As the amount of plastic deformation of the layer being removed is significantly decreased with the use of such tools, CCR also decreases as a direct result. As such, much thinner and longer chip is produced. The handling of such a coiled chip presents a serious problem in industry. Therefore, increasing uncut chip thickness or $t_1$ must be necessary to increase the chip thickness $t_2$, and thus its breakability.

**Conclusion**

The rake angle in OMC is among the most powerful mean to affect the deformation of the layer being removed in its transformation into the chip. It is proven that the influence of the rake angle on this plastic deformation is due to the fact that this angle directly affects the state of stress triaxiality in the deformation zone. The higher the rake angle, the smaller deformation of the layer being removed in OMC.

Although the use of high rake angles improves tool performance in terms of reducing the cutting force and energy spent in cutting, two new problems, namely, the chip length and possible reduced tool life may arise.

The problem with chip length and its breakability may be solved by increasing the uncut chip thickness. When this solution is applied, not only the chip breakability improves but also the machining efficiency increases. This is because the uncut chip thickness is directly correlated with the cutting feed, and thus with the tool penetration rate that determines the machining process efficiency. As such, the unit energy spent on the plastic deformation of the layer being removed decreases.

The major problem for researchers and tool developers in the field of metal cutting and tool design is that the influence of the tool geometry parameters on the state of stress (and thus the process machinability) is intertwined so it is impossible to study one parameter at a time. Only when a realistic FEM model of metal cutting is applied and the state of stress in the deformation zone is taken into consideration, the finding of the evaluation of the optimality of the insert geometry for a given application can be performed easily. The number of shapes of the rake face of the indexable inserts can be greatly reduced and the optimal shape for a given cutting conditions can be determined by FEM modeling.
References


