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RESEARCH

Friction and Material Modelling in Finite Element Simulation of Orthogonal Cutting

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A B S T R A C T

In the present paper the influence of the friction and material modelling on the results of the Finite Element simulations of machining is investigated. An orthogonal cutting model is proposed, which incorporates Coulomb's friction law. The validity of this model is tested against similar experimental and numerical results from the relevant literature and the influence of the friction coefficient is investigated. Then, a second model, with a friction model based on Zorev's stick-slip theory, is prepared and compared to the first one. Furthermore, simulations with Johnson-Cook material model for both kinds of friction modelling are presented and compared to the other models. The results of the different kinds of models although exhibit small discrepancies between models' results such us cutting forces, affect temperatures and chip morphology.

1. INTRODUCTION

Analytical models, statistical methods [1], numerical modelling and especially the Finite Element Method (FEM) are widely used for the analysis and the prediction of the cutting performance in machining operations [2,3]. Simulations of orthogonal cutting using the finite element method have a background of about three decades [4,5]. With the increase of computer power and the existence of commercial FEM software, this method has proved to be the favourite modelling tool for researchers of the field. This is established by the vast number of publications on this subject as well as the modelling novelties introduced and used, even by the fact that software dedicated solely for the purpose of cutting modelling exist. Results of the aforementioned analyses are important values such as cutting forces or temperatures on cutting tool and workpiece that are very hard to be experimentally measured or predicted otherwise.

The success of an orthogonal cutting FEM model depends on many parameters such as the model formulation used, mesh, element types and boundary conditions applied, and the use of modelling techniques such as adaptive meshing and chip breakage characteristics [6]. However, of great importance in order to produce a sound and realistic simulation are the friction and the material laws applied to the model. Friction modelling at the secondary deformation zone, at the chip and the tool rake face interaction area, is of importance when machining modelling is studied. In machining operations there are severe contact conditions between the tool and the chip, especially for turning operations that the interaction between those two elements is long. Many researchers claim that the discrepancies between experimental and FEM results are attributed to failure of providing an adequate friction model. Friction characteristics in the tool-chip interface are difficult to be experimentally determined; few methods, e.g. pin-on disc friction test, are available to identify friction parameters. In cutting operations, matters are perplexed due to phenomena taking place at the tool chip contact area; strain hardening and thermal softening are mechanisms acting simultaneously while the role of cutting fluids is to be studied too.

Material modelling pertains to the flow characteristics of the workpiece material and the corresponding equations to be included in the FEM model. These constitutive equations describe the flow stress or instantaneous yield strength at which work material starts to plastically deform or flow; the elastic strains are much lower than plastic strains in metal cutting and so workpiece material flows plastically into the cutting zone. Machining conditions subject workpiece material to high levels of strain, strain rate and heat which greatly influence flow stress. In the primary zone strain and temperature ranges from 1-2 and 150 ^oC - 250 ^oC respectively and in the secondary deformation zone from 3 to much higher and 800 ^oC - 1200 ^oC, while strain rates reach values of up to $2x10^4$ s^{-1} and 10^5 s^{-1} in the two zones [7]. Lack of data for high stresses, strain rates and temperatures as the ones encountered in machining is a major drawback. In many cases the constitutive data are taken from standard tension tests that are not sufficient for machining processes. Dynamic experimental material tests such as Split Hopkinson Pressure Bar (SHPB) impact testing is employed. However, the results are not sufficient for the deformation behaviour of metals, especially in high speed machining; values beyond test results are calculated by interpolation. Astakhov and Outeiro criticized the use of SHPB results in machining [8]. They argue that the available data are not from specialized laboratories, generally speaking SHPB requires special equipment and it is not clear how to correlate uniaxial impact testing results of SHPB with materials that are triaxially stressed, as in metal cutting. Other tests used are torsion tests, compression ring tests and projectile impact tests [9].

The aim of this study is build a FEM model of orthogonal cutting, the validity of which is to be tested with models and experimental data already published, and then two different friction models, namely Coulomb and Stick-slip friction model will be applied. The former classic model is well known and extensively used in the relevant bibliography due to its simplicity [10]. However, it has been criticized by many researchers. The latter is based on Zorev's theory and it assumes a transitional zone within some distance from the tool tip that is the onset of the transition from sticking to sliding region and is also popular among researchers [11]. Furthermore, two different material model schemes are used. In the first model, workpiece material properties are incorporated into the model from the available software data [12]. In another approach, the Johnson-Cook model is inserted into the simulation and results are compared [13].

For the aforementioned models a commercial FEM software was used, namely MSC Marc®. Different models with varying friction and material characteristics were studied and cutting forces, temperatures and chip geometry were compared. The results of the analysis exhibit that the models show little discrepancies in numerical results.

2. FINITE ELEMENT MODELING

For the analysis, a 2D orthogonal cutting finite element model is developed with the aid of the commercial FEM code MSC Marc®. Although there are commercial FEM codes specially designed for simulating machining operations, e.g. Third Wave AdvantEdge®, this particular software allows only Coulomb friction modeling;

for the purposes of the present analysis MSC Marc® was considered more suitable. The proposed model is a coupled thermo-mechanical, Lagrangian one. For chip formation there is no need for a separation criterion; however, when a predefined threshold value of tool penetration occurs, remeshing is applied. With the aforementioned technique, chip formation is performed smoothly and no large distortions of the original mesh are allowed. The workpiece material properties are imported into the model from the code's database; flow curve is inserted in the form of strain, strain rate and temperature depended tables.

In order to compensate for the thermal softening effect, low cutting speed and feed rate are applied. Furthermore, no cutting fluid is considered. The cutting conditions incorporated into the model are feed rate of f=0.05 mm/rev, a cutting speed of 125 rpm and depth of cut a=1.45 mm. Workpiece material is C15 steel. The cutting tool is modeled as rigid made of high speed steel with rake angle of α=25°, clearance angle of 5° and edge radius of r=0.002 mm. The workpiece model is 2 mm long and 0.5 mm high. In the beginning of the analysis it possesses 400 4-noded elements and 451 nodes, while with the re-meshing procedure it reaches 5000 elements. The selected workpiece and tool geometries and cutting conditions are similar to the ones presented in [12]. Thus, the obtained numerical results of the proposed model can be compared to the experimental and numerical results presented in another study.

For the model of this section, the case of classical friction situation following Coulomb's law is assumed; frictional sliding force is proportional to the applied normal load. The ratio of these two is the coefficient of friction μ which is constant in all the contact length between chip and tool. The relation between frictional stresses τ and normal stresses may be expressed as:

$$\tau = \mu\sigma \tag{1}$$

The friction coefficient is taken as 0.4 constant throughout the analysis in all the contact length. In Fig. 1, the strain rate in the workpiece and the chip can be observed. Furthermore, the chip morphology can be seen.

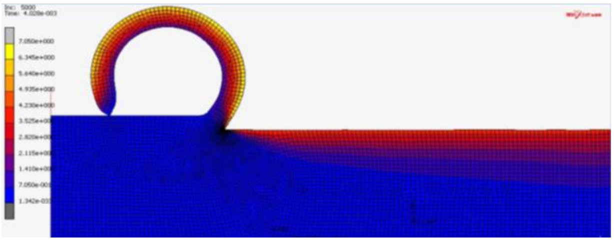


Fig. 1. Plastic strain in workpiece and chip.

In Fig. 2 the cutting force component for the experimental and FEM results of [12] and the FEM results of the proposed model are depicted.

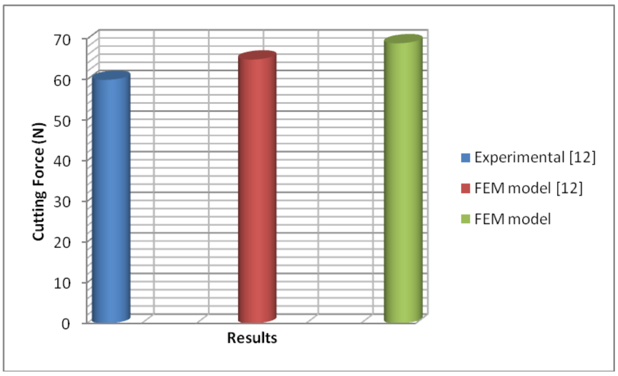


Fig. 2. Cutting force results.

By comparing the values of the forces it can be said that they are in good agreement. The same cannot be said for the thrust force components. Both numerical results from [12] and the model of this paper are incomparable to the experimental one. More specifically, the thrust force was measured to be 50N while the predicted values of the model from reference [12] and the proposed model are 9N and 12N, respectively. The failure to accurately predict both force components is attributed to two different reasons. In the first case, it is argued and backed-up with experiments that the discrepancies between modelling and experimental results lay with the materials and the conditions and not with the failure of software to simulate machining. It is agreed that cutting and thrust forces are not correctly predicted at the same time, the latter being underestimated [14]. In the second case, the discrepancies are attributed to inadequate workpiece material or contact conditions modelling. The workpiece material modelling may not work well at high stresses, strain rates and temperatures as the ones encountered in machining and Coulomb's law may not describe well the friction conditions in the chip-tool interface.

3. RESULTS AND DISCUSSION

3.1 Friction modelling

Modelling and simulation is of great importance in engineering [15,16]. Similarly, the evaluation of friction models in machining has been the topic of a number of publications. In a reverse engineering approach, five different friction models were tested and the results were compared against experimental results to decide which friction model is the most suitable [17]. The results were best when friction models with variable shear stress and coefficient of friction were incorporated with the finite element models. Furthermore, an ALE model was used to measure the influence of friction models on several parameters [11]. On the implementation of the stick-slip model it is concluded that a major disadvantage is the uncertainty of the limiting shear stress value. In another work [18], five different friction models were analyzed and the investigators concluded that mechanical result, e.g. forces, contact length, are practically insensitive to friction models, as long as the "correct" friction coefficient is applied, while on the other hand, friction modeling greatly affects thermal results. In [19] an improved friction law formulation is suggested where the constant friction coefficient is replaced by one which increases with plastic strain rate. Another parameter, which is closely connected to friction and FEM modeling, the contact length, is analyzed in [20]. Several contact length models utilized in the prediction of contact length in machining are analyzed. It should be noted that several papers presume frictionless contact in the chip-tool interface. Finally, it is observed that in several experimental data provided in the relevant literature, friction coefficients are well above the value of 0.577; above this value no relative motion at the tool-chip interface can occur [21]. It is assumed [22] that friction coefficients above 1 need the strongest levels of adhesion between asperities and the tool; these conditions may be encountered at the newly formed chip and at high temperatures as those in the chip-tool interface.

In this section the model of section 2 is used with various friction coefficients. All cutting conditions, workpiece and tool geometries and model parameters are kept the same except friction coefficient, in order to evaluate its effect on model results. In Table 1 the cutting and thrust force components and temperatures are presented.

Table 1. Influence of friction coefficient on force components and maximum temperature.

Model No.	Friction factor [-]	Cutting force [N]	Thrust force [N]	Temperature [^{o}C]
1	0.2	63	24	350
2	0.4	69	12	544
3	0.5	70	11	558
4	0.7	77	4	603

From the results of Table 1 it can be concluded that with increasing friction coefficient cutting forces and temperatures increase while thrust forces decrease. It can also be said that thrust forces and temperatures are affected more than cutting forces. In all the models tested, the chip morphology is marginally affected, resembling the chip of Fig. 1, corresponding to model number 2 in Table 1.

However, as the normal stresses increase and surpass a critical value, Coulomb's equation fails to give accurate predictions. From experimental analysis it has been verified that two contact regions may be distinguished in dry machining, namely the sticking and the sliding region. Zorev's stick-slip temperature independent friction model is the one commonly used [23]. In this model there is a transitional zone with distance ℓ_t from the tool tip that signifies the transition from sticking to sliding region. Near the tool cutting edge and up to ℓ_t, i.e. the sticking region, the shear stress is equal to the shear strength of the workpiece material, k, while in the sliding region, the remainder of the contact length ℓ_c, the frictional stress increases according to Coulomb's law.

This can be formulated as:

$$\tau = \begin{cases} k, 0 < \ell \leq \ell_t \\ \mu\sigma, \ell_t < \ell \leq \ell_c \end{cases} \tag{2}$$

In order to implement this equation to the model, experimental results from the literature and the theory are followed [11], [18]. More specifically, for the sticking region the shear friction factor m is calculated as the quotient of the frictional shear stress to the shear flow stress of work material at the tool-chip interface.

Shear friction factor is constant in the sticking region; for the sliding region Coulomb's friction law is applied with a value for friction coefficient equal to 1, calculated by:

$$\mu = \frac{F_t + F_c \tan\alpha}{F_c - F_t \tan\alpha} \quad (3)$$

A model is constructed with the above mentioned characteristics and with shear friction factor equal to 0.4. In Fig. 3 the chip morphology and the plastic strain on workpiece and chip are depicted. The cutting force and thrust force components are calculated as 74 N and 7 N respectively and the temperature is estimated at 628 °C.

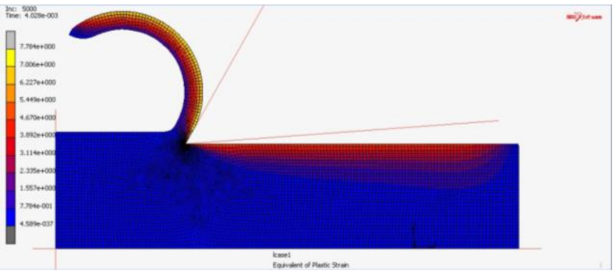


Fig. 3. Chip morphology and plastic strain.

Although the chips in Figs. 2 and 3 are at the same cutting length from the right corner of the workpiece, it can be seen that the first one is significantly more curved than the second. Furthermore, the cutting force component is slightly higher, at about 7 %, while thrust force component is almost half. It is worth noticing, that the thrust force in this model has also the opposite direction from the related force component of the model of the previous section. Temperature is also about 15 % higher in the model with stick-slip friction conditions.

Additionally, two models are constructed with two different values of shear friction factors, namely with values 0.2 and 0.6. The results on cutting force components and maximum cutting temperature are shown in Table 2.

Table 2. Influence of shear friction factor on force components and maximum temperature.

Model No.	Friction factor [-]	Cutting force [N]	Thrust force [N]	Temperature [°C]
5	0.2	62	-14	341
6	0.4	74	-7	628
7	0.6	86	25	887

In the thrust force column the minus sign in the first two values denotes the opposite direction of the forces in comparison to the third value. Figure 4 shows the experimental value of the cutting force in comparison to the predicted values of the same component with Coulomb friction model and stick-slip friction model, namely of model number 2 and model number 6. Although results are not directly comparable, due to different modelling conditions, it can be seen that the second model overestimates the value of the cutting force; however, differences are quite small.

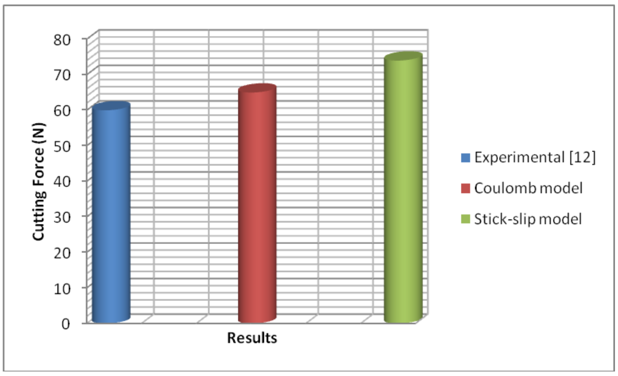
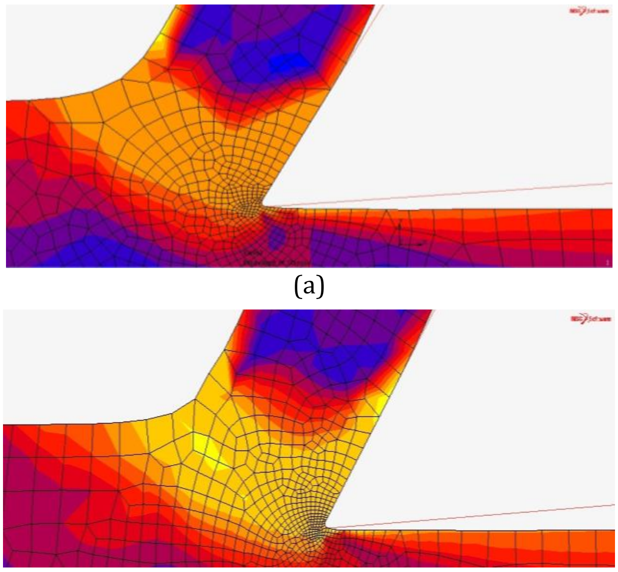


Fig. 4. Comparison of cutting force results.

Figures 5 (a) and (b) show the stresses in the primary and secondary deformation zones for model with (a) Coulomb friction and (b) stick-slip friction considered. In the same Figures, the re-meshing procedure, especially near the cutting tip, can be observed.



(a)

(b)

Fig. 5. Stresses in the primary and secondary deformation zone when (a) Coulomb (μ=0.4) and (b) stick-slip (m=0.4) model is considered.

It can be seen from the Figures and it was measured from the models that the second chip is thicker, namely 0.1 to 0.08 mm in comparison to the first chip. Furthermore, the second chip presents higher contact length with the cutting tool in comparison to the second one, 0.08 to 0.06 mm respectively. Both these observations are anticipated and attributed to the friction model used.

3.2 Material modelling

Although many constitutive equations have been employed for the case of metal cutting, only some are discussed here. The first is the relation by Usui, Maekawa and Shirakashi [24,25]:

$$\sigma = B\left[\frac{\dot{\varepsilon}}{1000}\right]^{M} e^{-kT}\left[\frac{\dot{\varepsilon}}{1000}\right]^{m}\left\{\int_{Path} e^{kT/N}\left[\frac{\dot{\varepsilon}}{1000}\right]^{-m/N} d\varepsilon\right\}^{N} \quad (4)$$

In this equation B is the strength factor, M is the strain-rate sensitivity and n the strain hardening index, all functions of temperature T, and k and m are constants. The integral term accounts for the history effects of strain and temperature in relation to strain-rate. In the absence of these effects, equation (4) is reduced to [26]:

$$\sigma = B\left[\frac{\dot{\varepsilon}}{1000}\right]^{M} \varepsilon^{N} \quad (5)$$

Oxley suggested a relation for carbon steel as [27]:

$$\sigma = \sigma_1 \varepsilon^{n} \quad (6)$$

with σ_1 the material flow stress for ε=1 and n is the strain hardening exponent.

As pointed out in section 2, material properties for the model are taken from the software database. However, when the limits of the data range are surpassed, the program uses the data at the extreme instead of performing an extrapolation or using an analytical formula. In this section a model is built with the material following the Johnson-Cook model [28]. The equation consists of three terms the first one being the elasto-plastic term to represent strain hardening, the second is viscosity, which demonstrates that material flow stress increases for high strain rates and the temperature softening term; it is a thermo-elasto-visco-plastic material constitutive model, described as:

$$\sigma = \left(A + B\varepsilon^{n}\right)\left[1 + C\ln\left(\frac{\dot{\varepsilon}}{\dot{\varepsilon}_o}\right)\right]\left[1 - \left(\frac{T - T_a}{T_m - T_a}\right)^{m}\right] \quad (7)$$

where $\dot{\varepsilon}_o$ is the reference plastic strain rate, T_α the ambient temperature, T_m the melting temperature and A, B, C, n and m are constants that depend on the material and are determined by material tests [29,30] or predicted [31].

Two models with Johnson-Cook material model, one considered with Coulomb and the other with stick-slip friction are built and the results of the maximum cutting and thrust forces and temperature are tabulated in Table 3.

Table 3. Influence of the Johnson-Cook model on force components and maximum temperature.

Model No.	Friction model	Cutting force [N]	Thrust force [N]	Temperature [°C]
8	Coulomb	66	12	401
9	Stick-slip	74	-7	546

Model number 8 in Table 3 is similar to model number 2 of Table 1 and model number 9 is similar to model 6 of Table 2, with only difference that models 8 and 9 are considered with Johnson-Cook material modeling. It can be seen that the corresponding models present only marginal discrepancies, if any, pertaining to cutting and thrust forces. However, it is worth noticing that in both cases, the maximum cutting temperatures are quite lower when Johnson-Cook model is used. In Fig. 6 the maximum cutting forces and temperatures for models number 2, 6, 8 and 9 are shown together for comparison of the results.

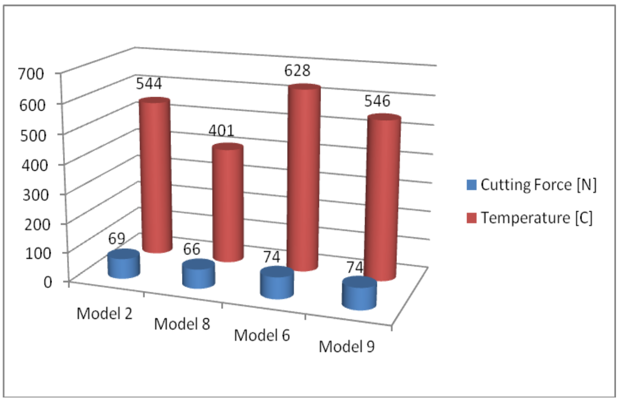


Fig. 6. Cutting forces and temperatures in selected models.

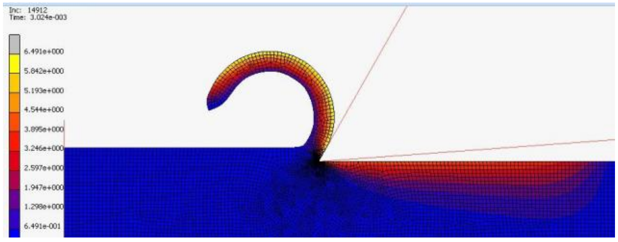


Fig. 7. Chip morphology and plastic strain in the simulation with Johnson-Cook material model.

In Fig. 7, plastic strain and chip are shown for model number 8. Chip morphology and strain contours appear to be similar between models 2 and 6 to 8 and 9, respectively.

4. CONCLUSION

In this paper the effect of two commonly used friction modeling approaches and two different material modeling proposals in Finite Element results was investigated. First, a 2D thermo-mechanical FEM model of orthogonal cutting was prepared and validated with experimental and numerical results from references with similar cutting conditions and geometrical characteristics. This model included the rather simple but commonly used Coulomb law for friction. Then, four similar models, but with different friction coefficients, were constructed and the influence of the coefficient on the predicted results was investigated. It was concluded that the variation of friction coefficient differentiates results and affects thrust force component and temperatures more than it affects cutting force.

Another friction modeling scheme that is usually encountered in orthogonal cutting FEM simulations is the one based on Zorev's stick-slip theory. Three more models were prepared based on the above mentioned theory and variations were observed not only on the force components values and maximum cutting temperatures but also on the chip flow and form. Once again it was found that friction modeling affects thrust force and temperatures more than cutting force; the same conclusions are found in other papers, too [11], [17]. Then, a new model, where the Johnson-Cook material model is incorporated was prepared. Similar conclusions were drawn pertaining to material modeling; temperatures are mostly affected by the application of this change in the model.

It is finally concluded that, at least in the case investigated here with low cutting speed and feed rate and without use of cutting fluid, friction and material modelling do not significantly affect the predictions of cutting forces. However, temperatures, especially in the region close to the cutting tool tip where maximum values are observed, are affected by the selection of friction coefficient, friction and material modelling applied.

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