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Modelling orthogonal machining of carbon steels. Part II: Comparisons with experiments

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ABSTRACT

Experimental measurements of cutting and thrust forces and chip shear plane angles have been carried out for six steels turned at a feed of 0.25 mm/rev at speeds from 50 to 250 m/min. The results have been successfully reproduced by finite element simulations. For this it has been necessary to include an initial yield drop from an upper to a lower yield stress in the description of the steels' plastic strain hardening. It has also been necessary to assume flow stress to reduce non-linearly with increasing temperature in the manner proposed by Zerilli and Armstrong, up to a temperature $\approx 900\,^{\circ}$C above which rapid softening takes place. A comparison is made between the present work and the earlier work of Oxley and his group.

1. Introduction

Part I of this paper [1] showed that finite element simulations of the machining of a softened carbon steel could be brought into improved agreement with experiments by including a description of an upper yield point in the steel's constitutive equation. This was presented as a general result, applicable to all such steels. It was not attempted to predict the behaviour of any particular steel. That would have required a consideration of its flow stress dependence on temperature and strain rate too.

In this, Part II, paper, attention is turned to simulating the behaviour of six particular steels. The paper includes experimental measurements of the steels' flow stress dependences on strain, strain rate and temperature. Flow stress at high strain rate reducing linearly with temperature was observed, as is frequently assumed in machining simulations and was first reported by Johnson and Cook [2]. The data were at first fitted to the power-law (PL) form of constitutive equation (Eqs. (3) and (4) of Part I). Simulations with this constitutive equation were not in agreement with experiments. It was also observed that predicted temperatures were higher than those used in the flow stress determinations. Excellent agreement with experiments could be obtained by both including the upper yield point and modifying the high temperature dependence of flow stress on temperature after the manner introduced by Zerilli and Armstrong [3]. Their work and developments from it used in this paper are reviewed in Section 2.

Agreements between experiments and simulations are checked in three different ways. Most directly, the predicted and experimental quantities F_C, F_T and ϕ are compared. These are, respectively, the cutting and thrust forces per unit cutting edge engagement length and shear plane angle. ϕ is obtained (Eq. (1)) from the chip thickness ratio t/a_c, where t is the chip thickness and a_c the undeformed chip thickness, and the tool rake angle γ. The predicted and experimental dependences of $(\phi-\gamma)$ on λ are also compared. λ is the direction between the resultant force and the normal to the rake face i.e. the friction angle that may be calculated from Eq. (2). This method of comparison is helpful because it gives guidance as to reasons for differences between simulated and experimental values of F_C, F_T and ϕ, should there be any [4]. Finally, comparisons are made between predicted and experimental shear stresses k calculated to act on the primary shear plane (Eq. (3)), also because of insights that this gives.

$$t/a_c = \cos(\phi-\gamma)/\sin\phi \tag{1}$$

$$\lambda = \tan^{-1}(F_T/F_C) + \gamma \tag{2}$$

$$k = [(F_C\cos\phi - F_T\sin\phi)\sin\phi]/a_c \tag{3}$$

It might be thought that there is no longer a need for research into the prediction of chip form and forces in the orthogonal machining of carbon steels. However, in much previous finite element work, for example [5–8], agreement between simulation

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and experiment has been reported in only up to two out of three of cutting and thrust force and shear plane angle; or it has not been attempted to obtain all three. It is common that if the (active) cutting force is correctly predicted, the (passive) thrust force is not. This paper's contribution is to present simulations in which all three are obtained in near-agreement with experiment. It also proposes a practical way to obtain the constitutive equations of other steels that have not been studied here that is the necessary input to the simulations.

There is an earlier analytical machining literature, associated with the name of Oxley [9], with particular researches jointly with Hastings et al. [10] particularly relevant to this paper. The present paper's results in relation to these earlier studies are discussed.

2. Temperature and strain-rate modelling

Eq. (4a) shows Zerilli's and Armstrong's [3] proposed constitutive equation for b.c.c. metals (T_{abs} is absolute temperature, and the strain-hardening exponent defined as $1/n$ in this paper was written n in the original). Subsequently [11], it has been proposed that the strain-hardening part should have a temperature dependence, Eq. (4b), through the temperature dependence of the elastic modulus E. In neither, as recognised in [3], is the high temperature recovery of strain hardening allowed for. For that reason, in this paper, a further modification is made. Above a critical temperature T_c (°C), the strain-hardening term is assumed to reduce linearly to zero as temperature increases to the melting temperature T_m (Eq. (4c)). This is the same empirical modification as used in the power-law form of Part I. A cut-off strain $\bar{\varepsilon}_c$ is also assumed in all cases, above which no further strain hardening takes place. Eq. (4d) is the modification for the case of Eq. (4a). Finally, the upper yield point continues to be modelled as in Eq. (4e):

$$\bar{\sigma} = \left(C_1 + C_5\bar{\varepsilon}^{1/n}\right) + C_2 \exp\left[(-C_3 + C_4 \ln \dot{\bar{\varepsilon}})T_{abs}\right] \tag{4a}$$

$$\bar{\sigma} = \left(C_1 + C_5\bar{\varepsilon}^{1/n}\right)(E_T/E_{ref}) + C_2 \exp\left[(-C_3 + C_4 \ln \dot{\bar{\varepsilon}})T_{abs}\right] \tag{4b}$$

$$\bar{\sigma} = \left(C_1 + C_5\bar{\varepsilon}^{1/n}\right)(E_T/E_{ref})\left(\frac{T_m - T}{T_m - T_c}\right) + C_2 \exp\left[(-C_3 + C_4 \ln \dot{\bar{\varepsilon}})T_{abs}\right], \quad T > T_c \tag{4c}$$

$$\bar{\sigma} = \left(C_1 + C_5\bar{\varepsilon}_c^{1/n}\right) + C_2 \exp\left[(-C_3 + C_4 \ln \dot{\bar{\varepsilon}})T_{abs}\right], \quad \bar{\varepsilon} > \varepsilon_c \tag{4d}$$

$$\bar{\sigma} = \sigma_u, \quad \bar{\varepsilon} \leqslant \varepsilon_u \tag{4e}$$

Table 1
Zerilli–Armstrong coefficients C_3 and C_4 for annealed iron and carbon steels.

Material	C_3	C_4	Ref.
Pure iron	0.0070	0.00042	[3]
0.12% C steel	0.0042[a]	0.00022[a]	[12]
Pure iron	0.0051	0.00026	[13]
0.45% C steel	0.0061	0.00019	[14]

[a] Values calculated by present authors from data in [12].

There is published work on typical values of the coefficients C_1–C_5, for pure iron and carbon steels. Values of C_3 and C_4 that control temperature and strain-rate dependence are shown in Table 1. In this paper, simulations are presented using Zerilli–Armstrong (ZA) constitutive equations. C_3 and C_4 have been assumed to be the same for all the steels, namely mean values from Table 1, 5.6×10^{-3} and 2.7×10^{-4}, respectively. The other coefficients have been obtained experimentally. Simulations are also presented using the power-law equations (3) and (4) from Part I. Then, in addition to those with 'as measured' constitutive equation coefficients, thermal softening coefficients have been chosen to mimic Zerilli–Armstrong temperature dependence.

3. Experimentation

Three plain and three free-machining carbon steels (Table 2) have been the subject of the physical experiments and simulations. The free-machining steels were from experimental casts (see Acknowledgements) and their compositions in Table 2 are measured. The plain carbon steels were purchased from a stockist. Their compositions are nominal mid-range values. For all, the room temperature thermal conductivities and specific heats are from data in [15]. Their densities were assumed to be 7860 kg/m^3.

3.1. Physical experiments

Both machining and mechanical property testing have been carried out.

Machining tests: All six steels, in cylindrical bar form from 60 to 100 mm diameter, were turned on a lathe, without any cutting fluid, at cutting speeds from 50 to 250 m/min and a feed of 0.25 mm/rev. SPGN 120304 inserts were used. Their cutting edge radii were measured to be $15 \pm 5\,\mu$m. Their room temperature thermal conductivity, specific heat and density were 60 W/mK, 255 J/kg K, 12,900 kg/m^3, from [16]. They were held to present 6° side rake, 0° back rake and 75° approach angle to the work. A fresh cutting edge was used and a 5 mm length of bar was turned for every test. The axial, radial and circumferential components of force on the tool were measured with a Kistler dynamometer. Chip thickness ratios (from which ϕ was calculated) were obtained by weighing measured lengths of chips. The depth of cut was varied in a preliminary set of tests with 080M46 steel. All force components per unit depth of cut, and chip thickness ratios, were found to become independent of depth of cut, i.e. to approach plane strain conditions, at depths $\geqslant$1 mm. A depth of cut of 1 mm was therefore chosen for all subsequent tests. The cutting and thrust force components F_C and F_T to be compared with the results of the plane strain simulations were obtained,

Table 2
Work material compositions and thermo-physical properties at 20 °C.

Material	Composition (wt%) (balance Fe)									K (W/m K)	C (J/kg K)
	C	Si	Mn	P	S	Cr	Mo	Ni	Pb		
070M20	0.20	0.25	0.70	0.05	0.05	0.30	0.15	0.40	–	52	490
080M40	0.40	0.25	0.80	0.05	0.05	0.30	0.15	0.40	–	48	480
080M46	0.47	0.25	0.80	0.05	0.05	0.30	0.15	0.40	–	48	480
9SMn36	0.09	0.01	1.01	0.06	0.33	0.03	0.01	0.04	–	55	470
9SMnPb36	0.08	0.04	1.49	0.07	0.42	0.02	0.00	0.02	0.27	55	470
SAE1144	0.44	0.13	1.45	0.01	0.30	0.15	0.00	0.02	–	47	490