ENGINEERING TRANSACTIONS • *Engng. Trans.* • **60**, 2, 113–124, 2012
Polish Academy of Sciences • Institute of Fundamental Technological Research (IPPT PAN)
National Engineering School of Metz (ENIM)
SIXTY YEARS OF THE ENGINEERING TRANSACTIONS

Modelling the Peak Cutting Temperature During High-Speed Machining of AISI 1045 Steel

Timothy J. BURNS[1)], Steven P. MATES[2)], Richard L. RHORER[3)], Eric P. WHITENTON[3)], Debasis BASAK[4)]

National Institute of Standards and Technology
[1)] *Information Technology Laboratory*
[2)] *Material Measurement Laboratory*
[3)] *Engineering Laboratory*
100 Bureau Drive, Gaithersburg, Maryland 20899, USA
e-mail: timothy.burns@nist.gov

[4)] *Orbital Sciences Corporation*
21839 Atlantic Blvd., Dulles, Virginia 20166, USA

This paper presents new experimental data on AISI 1045 steel from the NIST pulse-heated Kolsky Bar Laboratory. The material is shown to exhibit a stiffer response to compressive loading when it has been rapidly preheated, than it does when it has been heated using a slower preheating method, to a testing temperature that is below the eutectoid temperature. It is argued, using a simple model for heat generation in the workpiece and the tool during machining, due to Tlusty, that this work has important implications for the modelling of high-speed machining operations. Based on the experimental data, a modification is recommended of the well-known Johnson-Cook constitutive model of Jaspers and Dautzenberg for this material, in order to achieve improved predictions of the peak cutting temperature in machining.

Key words: high-speed machining, thermal modelling, AISI 1045 steel, Kolsky bar.

1. Introduction

High-speed machining processes can cause extremely rapid plastic deformation and heating of the work material. If this material is a carbon steel, a small region of thickness on the order of 10 μm is deformed plastically in the primary shear zone, to a strain on the order of 100%, at a strain rate on the order of 10,000 s^{-1}, on a time interval on the order of 10 μs. Subsequently, the material is subjected to additional large plastic strain in the secondary shear zone for a time on the order of 1 ms. During this small cutting time, the work material undergoes a change in temperature on the order of magnitude of 1000°C. Thus, a heating rate on the order of one million degrees Celsius per second is not uncommon for iron-carbon alloys of interest in manufacturing (see, e.g., [13]). Under such extreme conditions, there can be insufficient time for thermally-activated processes, such as solid-solid phase transformations, dislocation annealing, and grain growth, to produce changes in the microstructure of the material that occur on significantly longer time scales; see e.g., [11]. This means that unique non-equilibrium superheated microstructural states can be present during high-speed machining operations, with the result that the material flow stress can differ significantly from that which is measured under equilibrium high-temperature conditions. This poses a major challenge for modelling the constitutive response of these materials for use in finite-element simulations of rapid machining operations; see, e.g., [5]. The focus of this paper is on the measurement and modelling of the constitutive response of AISI 1045 steel, for use in the study of high-speed machining operations, and in particular, for finite-element analysis (FEA) simulations of these processes, because this approach is gaining wide use among both academic researchers and manufacturers.

In the thesis of Jaspers [8], a systematic effort was made, for a number of metals of interest in manufacturing, first to identify the conditions of the workpiece material during a high-speed metal cutting operation, and second to develop material testing methods that could reproduce these conditions as closely as possible. A result of this work was the publication of what is arguably the most often used constitutive model for AISI 1045 steel [9], a five-parameter phenomenological Johnson-Cook model [10]. In the same paper, a six-parameter Zerilli-Armstrong model for the material was also published. Even though Jaspers concluded that this model provided a better fit to the experimental data than did the Johnson-Cook model, and even though the Zerilli-Armstrong model is better motivated from a scientific point of view, this model is mathematically more complicated than the Johnson-Cook model, and it is harder to fit experimentally. As a result, it is less widely available and less widely used in FEA simulations of machining processes. For these reasons, and for reasons that will become clear in what follows, attention in this paper will be focused on the Johnson-Cook model for AISI 1045,

$$\overline{\sigma}\left(\overline{\varepsilon},\dot{\overline{\varepsilon}},T\right) = \left(A + B\overline{\varepsilon}^{n}\right)\left(1 + C\ln\dot{\overline{\varepsilon}}\right)\left(1 - T^{*m}\right), \tag{1.1}$$

where the effective true stress is expressed as a simple product of functions of the effective true strain, strain rate, and temperature, respectively. The homologous temperature T^* is given by the nondimensional formula $T^* = (T-T_r)/(T_f-T_r)$, where T is the temperature of the material in degrees Celsius, $T_r = 20°\mathrm{C}$ is the reference temperature, and $T_f = 1490°\mathrm{C}$ is the melting temperature of the material. The parameters that were determined in [8, 9] for AISI 1045 are as follows:

$$\begin{aligned} &A = 553.1\ \text{MPa}, \qquad B = 600.8\ \text{MPa}, \qquad C = 0.0134, \\ &n = 0.234, \qquad m = 1.0. \end{aligned} \tag{1.2}$$

In this paper, new experimental data are presented on AISI 1045 steel from the NIST pulse-heated Kolsky Bar Laboratory [12]. It is shown that, when the material has been preheated to a temperature that is below the eutectoid temperature (723°C), it exhibits a stiffer response to compressive loading when it has been rapidly preheated, i.e., heated to a uniform temperature in a few seconds, than it does when it has been preheated using a slower method, as was done in the thesis work of Jaspers [8]. It is argued, using a simple model for heat generation in machining due to Tlusty [13], that this work has important implications for the modelling of high-speed machining operations. Based upon this work, a modification is recommended of the well-known Johnson-Cook constitutive model of Jaspers and Dautzenberg [9] for this material, in order to achieve improved predictions of the peak cutting temperature in finite-element analysis simulations of high-speed metal cutting operations.

In the next section, a brief review is given of some thermal imaging data that were taken during continuous chip formation in some steady-state orthogonal cutting experiments. Following this, a brief discussion is provided of Tlusty's model. The fourth section presents some relevant NIST pulse-heated Kolsky bar data to provide a possible explanation for why the Jaspers and Dautzenberg models underpredicted the temperature in the machining simulations. The final section uses these data to discuss a possible modification of the Johnson-Cook model for application to high-speed machining processes, along with some discussion and conclusions.

2. Review of analytical and experimental tool-chip interface temperature results

In a series of steady-state orthogonal cutting experiments on AISI 1045 steel that were performed at NIST [6], the temperature field along the tool-chip interface was measured under conditions of continuous chip formation. In four sets of these experiments, all of the cutting parameters were kept the same, except for the uncut chip thickness; see Table 1. Assuming conditions of plane strain and material incompressibility, the chip velocity was calculated, and then the net thermal flux Φ that exited a control volume surrounding the cutting region was estimated for each of the four sets of experiments. Assuming the net thermal energy flux was equal to the total mechanical power led to an estimate for the specific cutting energy K_s in the system,

$$\Phi = F_c v_c = K_s h b v_c. \tag{2.1}$$

Here, F_c is the cutting force, $v_c = 3.7$ m/s is the cutting speed, and $b = 1.5$ mm is the chip width. For the four different uncut chip thicknesses, $h = 23$ mm, $h = 31$ mm, $h = 40$ mm, and $h = 48$ mm, it was found that the specific cutting energy was nearly constant, with $K_s \approx 2400$ N/mm^2.

Table 1. Data from four sets of orthogonal cutting experiments; h and h_c are, respectively, the uncut and cut chip thicknesses, b is the chip width, and v_c is the cutting speed.

No	h [μm]	h_c [μm]	b [mm]	v_c [m/s]
1	48	160	1.5	3.7
2	40	145	1.5	3.7
3	31	125	1.5	3.7
4	23	100	1.5	3.7

In the same study, a transient advection-diffusion model for the temperature distribution in orthogonal metal cutting, which was originally developed by Boothroyd [2], and subsequently improved upon by Tlusty [13], was used to calculate the temperature field in the chip and in the tool for the same four sets of orthogonal cutting parameters, using a finite-difference numerical method. The stress in this model is determined directly from the specific cutting energy, and it does not depend upon the temperature. The model allowed for heat transport into both the tool material and uncut workpiece material. While these comparatively simple finite-difference calculations did not accurately reproduce the temperature contours measured in the cutting experiments, they gave remarkably good predictions of the peak temperature along the tool-chip interface; see Fig. 1.

In a subsequent study [7], the commercial finite-element software package ABAQUS [1] was used to model the temperature in these experiments. Using both the Johnson-Cook and the Zerilli-Armstrong material response models for AISI 1045 that had been developed specifically for computer simulations of metal-cutting operations by Jaspers (see [9]), it was found that the simulations underpredicted the peak tool-chip interface temperature by hundreds of degrees Celsius; see Fig. 2.

The comparisons of the finite-difference and finite-element analysis results with the experimental data support the hypothesis that there is insufficient time for thermal softening mechanisms to have much effect on the work material in the cutting region during high-speed machining, so that the material has a stiffer response than is predicted using standard constitutive models. In the present study, suggestions are made as to how to modify the Johnson-Cook constitutive model to achieve improved temperature predictions.

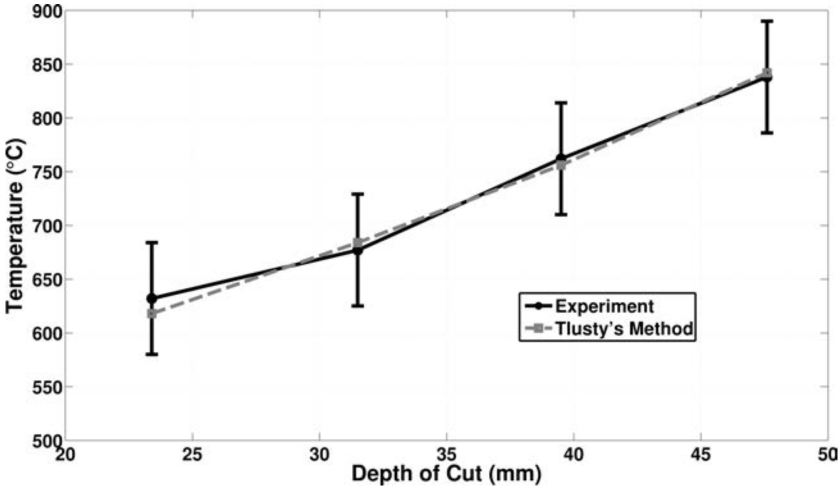

FIG. 1. Experimentally measured peak tool-chip interface temperatures compared with predictions of these temperatures using Tlusty's method; error bars denote an overall uncertainty (2σ) of $\pm 52°$C in the data [6].

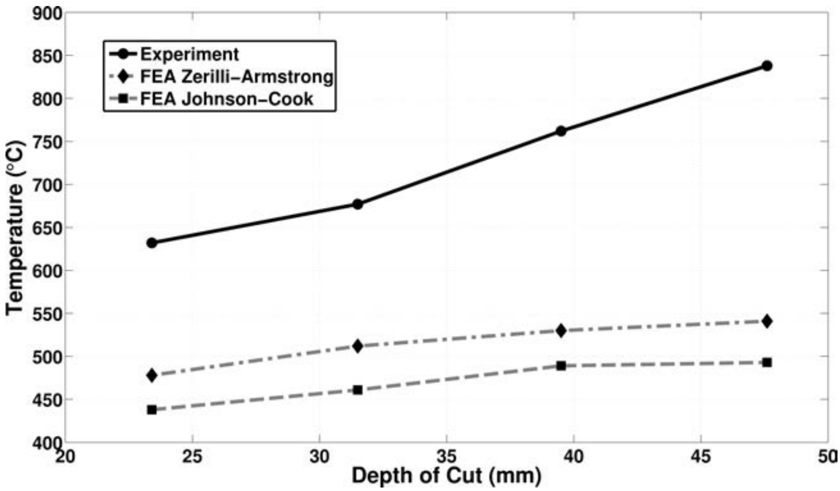

FIG. 2. Experimentally measured peak tool-chip interface temperatures compared with predictions of these temperatures by means of finite-element analysis.

3. Tlusty's advection-diffusion model

The finite-difference model for the tool-work material interface temperature, as presented by Tlusty [13], assumes that there are two heat sources, and that heat is transported by conduction in the direction normal to the tool-chip interface, and by mass transfer along with the work material in the direction of chip flow along the tool face. The first source of heating is represented by the shearing power, P_s, which arises from rapid dissipation by plastic deformation in the primary shear zone; this zone is modelled as a planar surface. This surface is assumed to be at a constant, uniform temperature, T_s. This temperature can be calculated using the following expression,

$$hbv_c\rho c\left(T_s - T_r\right) = P_s = F_s v_s. \tag{3.1}$$

Here, h and b are the depth of cut and chip width, respectively, as already specified in the preceding section; v_c is the cutting speed; $\rho = 7800$ kg/m^3 and $c = 474$ J/(kg-K) are, respectively, the density and specific heat of the workpiece material; $T_r = 20°$C is the reference temperature; F_s is the shearing force; and v_s is the shearing speed. The second source is the friction power, P_f, which is generated by friction along the chip-tool interface in the secondary shear zone, which is also modelled as a planar surface. The model for P_f is based on experimental tool pressure measurements [4]. Assuming that the orthogonal cutting parameters are known, including the friction angle β, the friction power P_f can be determined once F_s is known. Following Tlusty [13], in [6] it was assumed that the friction angle $\beta = 16.7°$ (i.e., tan$\beta = 0.3$). Thus, Tlusty's model predicts the tool-chip interface temperature by using the conditions on the primary shear plane, together with a model for the pressure along the tool chip interface. Furthermore, Tlusty's model predicts a shear plane temperature of approximately 600°C in AISI 1045 steel, and to a first approximation, this is independent of h, b, and v_c.

Now, suppose that the specific cutting energy for the material, K_s, is unknown. Then another method to calculate the shear force on the primary shear plane is to use the shear flow stress,

$$F_s = \tau_s L_s b. \tag{3.2}$$

In Eq. (3.2), τ_s is the shear stress on the primary shear plane, L_s is the length of the primary shear plane, and b is the chip width. Thus, given the orthogonal cutting parameters, if there is a good constitutive response model available for the stress in the work material, the cutting forces and temperatures of interest can be predicted using this simple model. Suppose that the constitutive response model for the effective true stress is given by the Johnson-Cook model, Eq. (1.1). Then, according to the von Mises criterion (see, e.g., Childs *et al.* [5]),

$$\tau_s = \overline{\sigma}\left(\overline{\varepsilon}, \dot{\overline{\varepsilon}}, T\right)/\sqrt{3}. \tag{3.3}$$

One way to interpret the results obtained in [6] using Tlusty's model is that the value of the temperature that should be used in Eq. (3.3) is approximately 600°C. A new experimental measurement of τ_s for AISI 1045, at a temperature that is close to 600°C, is discussed in the next section.

4. Nist pulse-heated Kolsky bar data

The split-Hopkinson pressure bar (SHPB), which is also called the Kolsky bar, is an experimental system that is widely used to determine the constitutive response of materials under conditions of rapid plastic deformation. A number of techniques have been developed for preheating a sample prior to impact testing in a Kolsky bar. The parameters for the Johnson-Cook constitutive model for AISI 1045 steel (Eqs. (1.1) and (1.2)), that was fit in the paper of Jaspers and Dautzenberg [9], were determined in part using data from a Kolsky bar apparatus, in which the samples were pre-heated in situ using a gas furnace, for a time on the order of a hundred seconds, to a temperature of up to 600°C, prior to loading in compression. At the National Institute of Standards and Technology, a unique SHPB facility has been in operation for several years. This laboratory combines a precision-engineered Kolsky bar and a controlled DC electrical pulse-heating system. The flow stress can be measured in samples that have been rapidly pre-heated to temperatures on the order of 1000°C, in a time on the order of one second, at heating rates of up to 6,000°C s^{-1}, and then rapidly loaded in compression at strain rates up to 10^4 s^{-1} [12].

4.1. AISI 1075

In recent work [3], pulse-heated compression test results on AISI 1075 steel were reported. The purpose of the experimental study was to investigate the magnitude of the difference in material strength that occurs in a carbon steel due to a transformation from the stronger bcc pearlitic structure to a structure that includes the less-strong fcc austentitic structure. The test samples had been carefully heat treated prior to testing, so that they had a uniform pearlitic microstructure. The particular alloy AISI 1075 was chosen for this study because it has the lowest austenization temperature, 723°C, among the carbon steels. In these tests, which were performed at a nominal strain rate of 3500 s^{-1}, each sample was pulse-heated to the test temperature within 2 s, held at temperature for a further 2.5 s, and then mechanically deformed to a true strain of approximately 0.25 to 0.35 within the next 100 µs. At temperatures above the austenization temperature (723°C) of the material, a nonequilibrium phase transformation from pearlite to austenite was observed to take place. At temperatures below the transformation temperature in this material, it was found that the material exhibited a stiffer response than is typically found in carbon steels. By fixing the value of the strain at 0.1, and the strain rate at 3500 s^{-1} in the Johnson-Cook model, Eq. (1.1), it was shown that the experimental results could conveniently be summarized by the following expression for the effective true stress vs. the temperature,

$$\overline{\sigma}\,(T) = 1140 \times (1 - T^{*m}) \quad [\mathrm{MPa}]. \tag{4.1}$$

What is interesting about these data is that, for experiments in which the material had been preheated to a temperature below the eutectoid temperature, a value of $m = 1.6$ was found to provide a good fit of the model in Eq. (4.1) to the data. This contrasts with the fact that typically, for carbon steels, SHPB tests in which the sample has been preheated more slowly prior to loading in compression, it is found that $m = 1.0$ (see, e.g., [9, 10]). Furthermore, for experiments in which the sample had been preheated to a temperature above the eutectoid, a value of $m = 0.7$ was found to provide a good fit of the model in Eq. (4.1) to the data. Thus, a Johnson-Cook type of model was found to be too simplistic to provide an overall good fit to the data. In addition, for the data on tests which were performed with preheating to a temperature below the eutectoid, a value of the thermal-softening parameter m greater than one is very interesting, because it supports the hypothesis that thermal-softening effects are less than would be expected to be found in experiments performed with a slower method of preheating the sample. This raises the question, does AISI 1045 steel exhibit a similar behaviour when it is pulse-heated, and then loaded in compression?

4.2. AISI 1045

Iron alloys with a smaller percentage of carbon, such as AISI 1045 steel, are used much more frequently than a spring steel like AISI 1075 in manufacturing processes that involve high-speed machining operations. As discussed in Sec. 3, Tlusty's model predicts a shear plane temperature of approximately 600°C in AISI 1045, which is below the lowest eutectoid temperature for an iron-carbon system. Could it be that one of the reasons that Tlusty's model outperformed the finite-element simulations in [7], in particular using the Jaspers-Dautzenberg fit to the Johnson-Cook model for AISI 1045, is that the actual material has a stiffer response, when it is rapidly heated to a temperature below the eutectoid, than was measured by Jaspers and Dautzenberg using their SHPB system? In other words, just as was described for AISI 1075 in the preceding section, does a value of the thermal-softening parameter m in the Johnson-Cook model that is greater than one provide a better fit to the pulse-heated experimental data than the value $m = 1$ reported in [9]? (Recall that a *larger* value of m corresponds to *less* thermal softening in Eq. (1.1)).

Figure 3 gives a plot of the true shear stress vs. true strain data from a pulse-heated Kolsky bar test that was performed at a nominal strain rate of 3600 s^{-1}. In this test, the sample was heated to a temperature of 640°C (with $\pm 2\sigma$ uncertainty of ± 20°C) in approximately one second, and then it was held at that temperature for approximately 6.2 seconds prior to compressive loading. Also shown in the figure are the uncertainty bounds ($\pm 2\sigma$) on the measurements

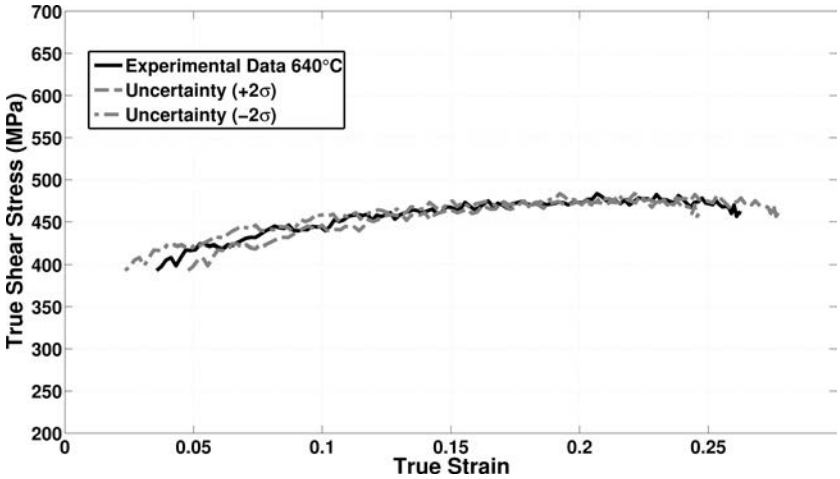

Fig. 3. Data (solid curve) from compression test of a AISI 1045 steel sample that had been pulse-heated to 640°C, and then plastically deformed, at a true strain rate of 3600 s^{-1}; uncertainty bounds ($\pm 2\sigma$) on true shear stress vs. true strain measurements are also plotted.

(see [12]). The same experimental data are plotted again in Fig. 4, along with two fits to the data, both obtained using the model of Jaspers and Dautzenberg at the same strain rate and temperature, but with $m = 1$ in the lower curve, and $m = 1.7$ in the upper curve. It is clear that the case with $m = 1.7$ provides a better fit to the experimental data, which means that the material exhibits

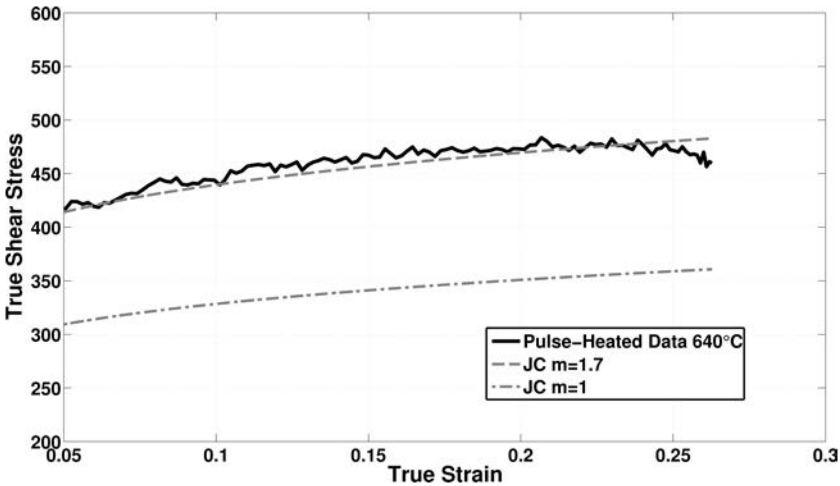

Fig. 4. Same data (solid curve) as in Fig. 3 are plotted, together with corresponding true shear stress vs. true strain values of the Johnson-Cook model for AISI 1045 of Jaspers and Dautzenberg; in the upper (dashed), and lower (dot-dashed) curves, $m = 1.7$ and $m = 1$, respectively.

a stiffer response to loading after it has been rapidly pulse-heated. Thus, this may provide at least a partial explanation for why Tlusty's method performed so much better in predicting the peak temperature along the tool-chip interface for the experiments that were reported in [6].

5. Discussion and conclusions

Experimental data on AISI 1045 steel have been presented, which show that the material exhibits a stiffer response when it has been pulse-heated, instead of preheated by a slower method, to a temperature below the eutectoid, prior to a dynamic SHPB compression test. These new data, together with the discussion of Tlusty's model in Sec. 3, imply that, for machining simulations, a value of $m = 1.7$, instead of $m = 1$, and a value of the temperature on the order of 600°C, are preferable for the Johnson-Cook model, Eq. (1.1), for FEA simulations of high-speed machining of AISI 1045. The work presented here supports the hypothesis that there is insufficient time for many significant microstructural changes to occur in the material during a high-speed machining operation. Thus, this may help to explain why the finite-element simulations of orthogonal cutting tests on this material were found to underpredict the peak temperatures measured in corresponding orthogonal cutting experiments [7].

As a final observation, the true shear stress vs. true shear strain data from Fig. 3 are plotted again in Fig. 5, along with the two plots of the Johnson-Cook model for AISI 1045 of Jaspers and Dautzenberg, where the maximum

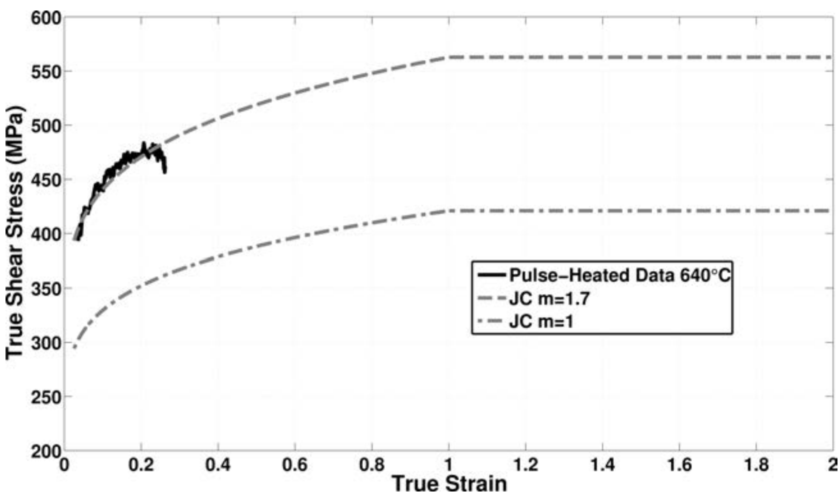

Fig. 5. Same data (solid curve) as in Fig. 3, plotted with values of the Johnson-Cook model for AISI 1045 of Jaspers and Dautzenberg, for true shear strains up to 2.0; it is assumed that n=0 for effective true plastic strains greater than 1.0.

strain is extended to 2.0, which is of the correct order for machining. For effective true strains greater than 1.0, it is assumed that there is no additional strain-hardening, i.e., $n = 0$; see CHILDS *et al.* [5]. This figure emphasizes that modelling of high-speed machining operations usually requires large extrapolations from data that have been obtained using currently available experimental methods, which typically means Kolsky bar (SHPB) tests. Ideally, constitutive data for machining simulations ought to be determined by means of some carefully designed cutting experiments.

Acknowledgment

This paper is an official contribution of the National Institute of Standards and Technology and is not subject to copyright in the United States. Commercial products are identified in order to adequately specify certain procedures. In no case does such identification imply recommendation or endorsement by the National Institute of Standards and Technology, nor does it imply that the identified products are necessarily the best available for the purpose.

References

1. *ABAQUS/Explicit Users' Manual*, Hibbit, Karlsson & Sorenson, Inc., Rising Sun Mills, 166 Valley Street, Providence, RI 02909–2499, 2003.
2. BOOTHROYD G., *Temperatures in orthogonal metal cutting*, Proc. IME, **177**, 29, 789–802, 1963.
3. BURNS T.J., MATES S.P., RHORER R.L., WHITENTON E.P., BASAK D., *Dynamic properties for modeling and simulation of machining: effect of pearlite to austenite phase transition on flow stress in AISI 1075 steel*, Mach. Sci. and Tech., **15**, 1–20, 2011.
4. BURYTA D., SOWERBY R., YELLOWLEY I., *Stress distributions on the rake face during orthogonal machining*, Int. J. Mach. Tools Manufact., **345**, 721–739, 1994.
5. CHILDS T.H.C., MAEKAWA K., OBIKAWA T., YAMANE Y., *Metal Machining: Theory and Applications*, Arnold Publishers, London, 2000.
6. DAVIES M.A., YOON H., SCHMITZ T.L., BURNS T.J., KENNEDY M.D., *Calibrated thermal microscopy of the tool-chip interface in machining*, Mach. Sci. and Tech., **7**, 2, 166–190, 2003.
7. DAVIES M.A., CAO Q., COOKE A.L., IVESTER R., *On the measurement and prediction of temperature fields in machining AISI 1045 steel*, CIRP Ann., **52**, 1, 77–80, 2003.
8. JASPERS S.P.F.C., *Metal Cutting Mechanics and Material Behaviour*, Ph.D. Thesis, Eindhoven: Technische Universiteit Eindhoven, 1999.
9. JASPERS S.P.F.C., DAUTZENBERG J.H., *Material behaviour in conditions similar to metal cutting: flow stress in the primary shear zone*, J. Mater Proc. Tech., **122**, 322–330, 2002.

10. Johnson G.R., Cook W.H., *A constitutive model and data for metals subjected to large strains, high strain rates and high temperatures*, 7th International Symposium on Ballistics, 541–547; The Hague, Netherlands, 1983.

11. Kaluba W.J., Taillard R., Foct J., *The bainitic mechanism of austenite formation during rapid heating*, Acta Mater., **46,** 16, 5917–5927, 1998.

12. Mates S.P., Rhorer R.L., Whitenton E.P., Burns T.J., Basak D., *A pulse-heated Kolsky bar technique for measuring flow stress of metals subjected to high loading and heating rates*, Exp. Mech., **48**, 799–807, 2008.

13. Tlusty J., Manufacturing Processes and Equipment, Prentice-Hall, Upper Saddle River, NJ, 2000.

Received June 1, 2011; revised version May 25, 2012.