Computational Materials Science 95 (2014) 568–578

Contents lists available at ScienceDirect

Computational Materials Science

journal homepage: www.elsevier.com/locate/commatsci

A new approach to the determination of plastic flow stress and failure initiation strain for aluminum alloys cutting process

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ARTICLE INFO

Article history:
Received 25 March 2014
Received in revised form 13 August 2014
Accepted 14 August 2014
Available online 16 September 2014

Keywords:
Flow stress
Aluminum alloys
Cutting
Failure initiation strain
Ductile failure

ABSTRACTS

Material flow stress in cutting is difficult to measure experimentally due to the complex deformation behavior involved. In this work, a finite element (FE) simulation of cutting and a theoretical flow stress model including plastic and failure criteria were developed to determine the flow stress during aluminum alloy (7075-T6) cutting process. In a stable plastic stage, the Johnson–Cook model considering the adiabatic temperature rise (ATR) was proposed to calculate the plastic flow stress, and the relative errors of the calculated and FE simulated stresses were less than 4%. In the ductile failure stage, an improved failure initiation criterion in terms of temperature, pressure and strain rate was proposed to determine the failure initiation strain in cutting. In addition, an energy-density based ductile failure criterion was used to predict the flow stress in the failure stage. By combining the proposed theoretical model results and the FE simulation results, the entire flow stress during cutting was obtained. The influence of the failure initiation strain of the chip layer on cutting performance was also discussed. In addition, a set of aluminum alloy (A2024-T351) orthogonal cutting simulations were performed to validate the effect of the failure initiation strain on the cutting process. The predicted variation trends of the failure initiation strain for the two types of aluminum alloys were similar, and the predicted forces and tool–chip contact lengths were compared with the measured values in Atlati et al. [9]. Finally, the prediction of the failure initiation strain and its influence on tool–chip contact behavior were validated indirectly.

1. Introduction

Metal cutting is one of the major manufacture processes that are most frequently used in the fabrication of industrial parts. It is estimated that approximately 15% of the value of all mechanical components manufactured in the world are produced by the cutting process [1]. A considerable amount of analytical or numerical works have been proposed to simulate metal cutting processes. Cutting force, temperature, chip formation, tool wear and surface integrity can be predicted by the finite element (FE) simulation. However, the flow stress model is normally the key factor for acquiring accurate simulation results. The flow stress in cutting is commonly a function of strain, strain rate and temperature [2]. Determination of the flow stress in cutting is extremely difficult due to the extreme characteristics of the workpiece material deformation, i.e., high strain (>1), large, and variable strain rate (10^3–10^6 s^{-1} or higher) and temperature (200–1000 °C or higher).

Many classic material models such as the Cockroft and Latham model [3], Zerilli–Armstrong model [4] and Johnson–Cook model [5] have been embedded in modern finite element analysis software to model material deformation including cutting. Material deformation in cutting commonly includes elastic, stable plastic and material softening or failure stages. Recently, many researchers have developed new material models based on classic material models to characterize material softening behavior, i.e., flow stress decreases with increasing strain, which exists following the stable plastic deformation stage in cutting. Calamaz et al. [1] introduced the strain softening effect into the original J–C model and developed a TANH model; the simulation results provided a good prediction of segmented chip morphology. Based on the J–C model and the TANH model, a series of temperature-dependent flow softening models were proposed to simulate Ti–6Al–4V alloy cutting concerning the effects of flow softening, strain hardening, thermal softening and their interaction [6]. In addition, a flow softening integrated material constitutive model was used to predict adiabatic shear band formation during machining [7]. Although most of the developed models can provide good prediction of chip morphology and cutting forces in the cutting simulation, the stress–strain curve cannot be validated directly because of the difficulty of flow stress measurement in cutting.

Alternatively, some researchers developed damage or material failure model to characterize the material softening phenomena.

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http://dx.doi.org/10.1016/j.commatsci.2014.08.029
0927-0256/

The J–C plastic model and ductile failure criteria including failure initiation and failure evolution laws have been proved to be an effective strategy for cutting simulation. Mabrouki et al. [8] utilized the J–C plastic model as well as an energy based ductile damage criterion to characterize the material behavior for an A2024-T351 alloy machining simulation. The chip morphology, temperature and residual stresses were predicted by the simulation. Similarly, the fracture energy based ductile damage criterion was applied to simulate chip formation and analyze the chip segmentation intensity ratio for A2024-T351 alloy cutting [9]. Zhang et al. [10] applied the fracture energy based ductile failure criterion in titanium alloy machining simulation to identify the limited shear stress at the tool–chip interface. The mesh characteristic length was found to have a great influence on the simulated cutting forces. Subsequently, an energy-density based ductile failure criterion was developed in a titanium alloy high-speed machining simulation, and the mesh dependence was reduced effectively [11].

Previous works indicate that both the flow softening and the damage modeling strategies can give a good description of flow stress decreasing behavior. Therefore, an integrated stress–strain response in cutting should include an elastic, stable plastic and a failure stages. However, very little work has been performed on the determination of material parameters to identify the stress–strain relationship in cutting [12].

There are two main strategies of determining the material parameters for the stress–strain equations in a cutting simulation, (1) the dynamic test technique; and (2) the inverse method for the optimization of modeling parameters. Gupta et al. [13] performed flat tensile tests to determine the material model parameters for stainless steel 316 alloy. Seo et al. [14] performed high temperature (200–1000 °C) dynamic compression tests on the split Hopkinson pressure bar (SHPB) system to identify the J–C plastic model parameters, and developed a modified J–C model to express the dynamic behavior of the Ti–6Al–4V alloy in the vicinity of the re-crystallization temperature. The split Hopkinson pressure bar (SHPB) technique was also applied to identify the KHL visco-plastic constitutive model parameters for titanium alloy [15]. In addition, other researchers performed high speed cutting experiments using a dynamic ballistic apparatus according to the SHPB test principles. For example, a ballistic apparatus was developed in terms of dynamic compression experiment technology, and high speed cutting experiments under a high strain rate condition were performed using this apparatus [16]. Hokka et al. [17] presented Ti-15-3 alloy deformation behavior over a large strain rate range (10^{-3}–3500 s^{-1}) using SHPB tests. Meanwhile, orthogonal cutting tests were carried out using "U-shape" specimens on a SHPB apparatus. In addition, the parameters of the flow stress expression for cutting can be identified by the inverse method by minimizing the errors of the simulated and measured cutting forces [18]. An "OXCUT" program was proposed to calculate cutting forces and temperatures of slot milling; meanwhile, an inverse optimization method was developed to identify material plastic model parameters [19]. The inverse method concerning experimental cutting forces is useful to coordinate the experimental and simulated conditions. However, the application of the method is confined due to the limited experimental cutting conditions.

The drawback of the dynamic test strategy is that the strain rate range of cutting is commonly larger than that of the dynamic test. Cutting deformation covers a large range of strain rate, from quasi-static to dynamic range, and also covers a large range of temperature, from ambient temperature to the high value close to the melting point. It is difficult to test the cutting flow stress over the entire ranges of strain rate and temperature through experiments. In addition, SHPB tests are commonly used to identify material plastic model parameters, but they cannot be applied to predict the failure process in cutting.

The present study aims at describing a method of determining the flow stress data in cutting by ductile failure method. The cutting flow stress is characterized by three stages: the elastic, stable plastic and ductile failure stages. The material plastic deformation behaviors considering adiabatic effects were analyzed, and the flow stresses at the ductile failure initiation and evolution stages were investigated by a coupled theoretical and numerical method.

2. Theoretical ductile material model for cutting

2.1. Plastic model

In this work, the Johnson–Cook (J–C) visco-elastic–plastic model developed by Johnson and Cook [5] was adopted as the basic rule to characterize the plastic deformation behavior of workpiece material in cutting. The constitutive equation is given by

$$\bar{\sigma} = [A + B(\bar{\varepsilon}^{pl})^n]\left[1 + C\ln\left(\frac{\dot{\bar{\varepsilon}}^{pl}}{\dot{\varepsilon}_0}\right)\right]\left[1 - \left(\frac{T - T_{room}}{T_{melt} - T_{room}}\right)^m\right] \tag{1}$$

where $\bar{\sigma}$ is the equivalent flow stress, $\bar{\varepsilon}^{pl}$ and $\dot{\bar{\varepsilon}}^{pl}$ are the equivalent plastic strain and strain rate, $\dot{\varepsilon}_0$ is the reference strain rate (1/s), T_{room} and T_{melt} are, respectively, workpiece ambient and melting temperature. A, B, n, C and m are material constants. These constants are usually determined by fitting the stress–strain curves obtained by SHPB tests. Commonly, SHPB tests are performed under adiabatic conditions. In the literature, the constitutive models are mostly based on isothermal flow curves, and the adiabatic effect should be corrected to isothermal flow curves [12]. The isothermal condition exists when the loading strain rate is lower than 1 s^{-1}, whereas the adiabatic temperature rise (ATR) generates at the dynamic loading condition when the strain rate is larger than 1 s^{-1} [20]. In machining, the strain rate in cutting zone is greater than 10^3 s^{-1} and can even be as high as 10^6 s^{-1}, so a fraction of plastic work is converted into heat due to energy conservation. The ATR of workpiece material generated by plastic power can easily reach several hundreds of degrees [17]. Thus, the ATR occurred in cutting plastic deformation can be directly obtained from the equation

$$\Delta T = \frac{\eta}{\rho C_p}\int_0^{\varepsilon} \sigma(\varepsilon)d\varepsilon \tag{2}$$

where η is the fraction of the plastic work that is converted into heat, and for most metals, the conversion factor is taken as 0.9 [21]; ρ and C_p are the density and heat capacity, respectively, of the material. Thus, the J–C plastic model considering the ATR is defined as Eq. (3) to calculate the flow stress with the increase of plastic strain in cutting deformation, theoretically.

$$\bar{\sigma} = \left[A + B(\bar{\varepsilon}^{pl})^n\right]\left[1 + C\ln\left(\frac{\dot{\bar{\varepsilon}}^{pl}}{\dot{\varepsilon}_0}\right)\right]\left[1 - \left(\frac{T_i + \Delta T - T_{room}}{T_{melt} - T_{room}}\right)^m\right] \tag{3}$$

where T_i is the initial temperature before cutting deformation.

2.2. Ductile failure criteria

In this work, the material softening behavior was characterized by the energy density based ductile failure (or damage) criteria. The failure criteria include failure initiation and evolution laws, and the failure initiation law is given by

$$\omega_{oi} = \int \frac{1}{\bar{\varepsilon}^{pl}_{oi}(\gamma, \dot{\bar{\varepsilon}}^{pl})} d\bar{\varepsilon}^{pl} \tag{4}$$

$$\bar{\varepsilon}^{pl}_{oi} = [d_1 + d_2\exp(-d_3\gamma)]\left[1 + d_4\ln\left(\frac{\dot{\bar{\varepsilon}}^{pl}}{\dot{\varepsilon}_0}\right)\right]\left[1 + d_5\left(\frac{T - T_{room}}{T_{melt} - T_{room}}\right)\right] \tag{5}$$