CIRP Annals - Manufacturing Technology 62 (2013) 695–718

Contents lists available at SciVerse ScienceDirect

CIRP Annals - Manufacturing Technology

journal homepage: http://ees.elsevier.com/cirp/default.asp

Recent advances in modelling of metal machining processes

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ARTICLE INFO

Keywords:
Modelling
Machining
Chip formation

ABSTRACT

During the last few decades, there has been significant progress in developing industry-driven predictive models for machining operations. This paper presents the state-of-the-art in predictive performance models for machining, and identifies the strengths and weaknesses of current models. This includes a critical assessment of the relevant modelling techniques and their applicability and/or limitations for the prediction of the complex machining operations performed in industry. This paper includes contributions from academia and industry, and is expected to serve as a comprehensive report of recent progress, as well as a roadmap for future directions. Process models often target the prediction of fundamental variables such as stresses, strains, strain-rates, temperatures etc. However, to be useful to industry, these variables must be correlated to performance measures: product quality (accuracy, dimensional tolerances, finish, etc.), surface and subsurface integrity, tool-wear, chip-form/breakability, burr formation, machine stability, etc. The adoption of machining models by industry critically depends on the capability of a model to make this link and predict machining performance. Therefore, this paper would identify and discuss several key research topics closely associated with predictive model development for machining operations, primarily targeting industry applications.

1. Introduction

1.1. Significance of modelling of machining processes

Conventional machining processes continue to occupy a dominant fraction of all manufacturing operations. New advances in machine tool and cutting tool technologies, along with advanced material development, all aimed at improved manufacturing productivity, product quality and cost reduction, require predictive performance models for use in process planning systems for machining processes. At the discrete part/product level, this includes selection of: (1) optimal cutting conditions; (2) coolant/lubricant type; (3) cutting tools, etc. The goal is to satisfy functional design requirements with an optimized process plan. To enable this, predictive performance models need to be developed and integrated with process planning. Development of advanced predictive models has accelerated to accommodate demands. These models can be broadly clustered as analytical, numerical, experimental, Artificial Intelligence (AI) based, and hybrid modelling techniques [309].

However, a dichotomy remains whereby predictive models focus on fundamental physical process variables such as stresses, strains, strain-rates, temperatures, and dynamic tool deflection. Alternatively, applications require prediction of process performance measures such as tool-wear/tool-life, surface integrity, cutting forces/power/torque, part accuracy, and process stability.

1.2. CIRP collaborative efforts in machining process modelling

The CIRP STC-C pioneered a new Working Group in 1995 on Modelling of Machining Operations, resulting in two major activities: (1) an on-going international workshop/conference series on modelling of machining operations; and (2) an extensive CIRP keynote paper on modelling of machining operations [309]. As a result, solid scientific grounds were set for fundamental and applied modelling research activities, with strong international collaboration. This has led to the application of operational-level predictions on the shop floor. Concurrently, fundamental analytical and numerical modelling serves as the basis for further predictive model development.

1.3. Predictive models for machining operations

Predictive models with simulation can be integrated into process planning systems to improve productivity and enhance product quality. Predictive performance models could also be effectively used in adaptive control for machining processes, reducing and/or eliminating trial and error approaches.

Industry is interested in process performance measures such as tool-life, surface finish, subsurface integrity, chip-form/chip breakability, burr formation, part accuracy, etc. Fig. 1 summarizes predictive modelling efforts. Quantitative input is used to predict output parameters in two distinct stages. Physics-based models

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0007-8506/$ – see front matter © 2013 CIRP.
http://dx.doi.org/10.1016/j.cirp.2013.05.006

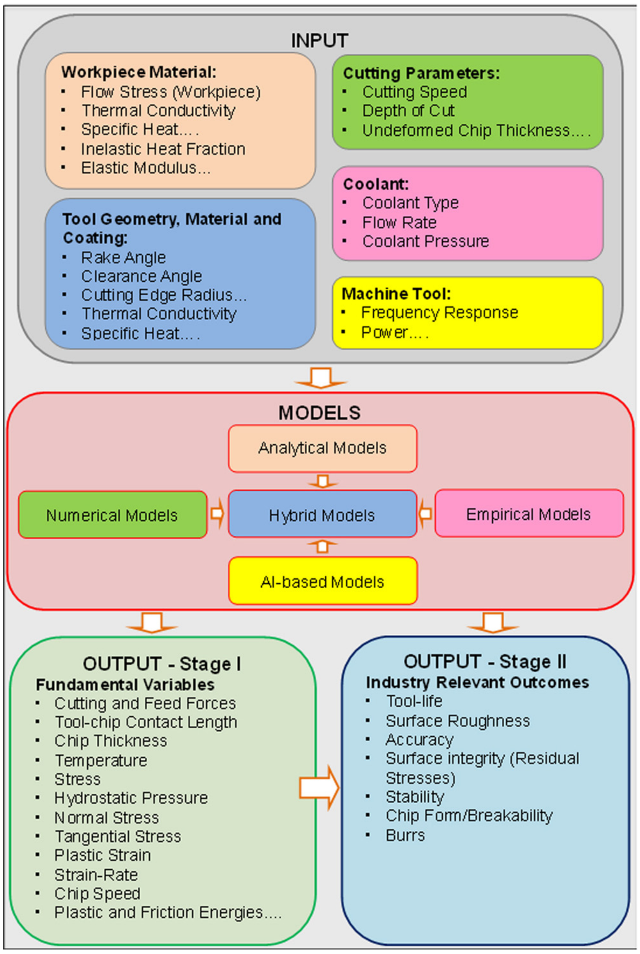

Fig. 1. Two-stage modelling approach adopted for machining processes.

are used to predict fundamental process variables, which are used to predict industry-relevant outcomes.

Predictive models span numerous methods. Analytical models, including slip-line models, can directly predict cutting forces, friction in the local cutting zones, stresses, strains, strain-rates and temperatures. However, the complexity of industrial processes still makes it difficult to predict all industry relevant outcomes analytically. Analytical models also provide useful input for optimising in numerical models. These numerical computational models can then offer realistic prediction of industry-relevant outcomes. This hybrid analytical/numerical approach has not been fully implemented, but is likely to be a major goal of future research.

1.4. Why modelling of machining?

Machining is the most prevalent manufacturing operation in terms of volume and expenditure. Machined components are used in almost every type of manufactured product. It has been estimated that machining expenditure contributes to approximately 5% of the GDP in developed countries, while in the U.S. alone it translates into approximately $250B per year [129]. Machining presents challenging and exciting intellectual problem that have fascinated researchers and practitioners for decades. Prediction of the fundamental physical variables by the methods shown in Fig. 1 has continued to make significant progress. However, we cannot ignore that the end goal of machining models is to predict industry-relevant outcomes, and thus improve productivity. Fig. 1 summarises the modelling efforts in recent times. Model inputs include cutting parameters, tool geometry, workpiece and tool materials. As mentioned in Section 1.3, from these inputs, most models calculate intermediary fundamental physical variables (Stage I). These are then used to obtain machining performance outcomes (Stage II). Without successful models, expensive experimental testing will continue to dominate practical process development. Thus, the most successful models, in terms of their adoption by industry, are those that successfully make the step from Stage I to Stage II; tool-life modelling is one such example. However, in the current state-of-the-art, while input variables appear to remain unchanged, process outcomes can still change drastically. This points to areas where there is still some fundamental lack of understanding and this emphasizes the need for continued fundamental modelling efforts.

1.5. Paper objectives and outline

This paper not only summarizes advances in modelling of metal machining since the last CIRP keynote paper on this topic in 1998, but also attempts to show where the jump from Stage I to Stage II has been observed. During the last 15 years, various research groups have made significant progress in modelling. These achievements are summarized in four major sections (Sections 2–5) followed by summary and outlook. Section 2 presents modelling advances in 2D and 3D chip formation, focusing on analytical and numerical models. Section 3 outlines the basic modelling requirements, including the relevant material constitutive and friction laws, along with parametric variables. Section 4 presents the progress in predicting the fundamental variables such as cutting forces, temperature, stresses, strains and strain-rates. Section 5 summarizes the prediction of industry-relevant process outcomes.

2. Advances in modelling of the chip formation process

2.1. Introduction

Due to its relative simplicity, modelling of orthogonal cutting continues to dominate research activities in machining. Most research focuses on calculating the fundamental physical variables in the chip formation process. While the end goal is prediction of Stage II outcomes, orthogonal cutting remains a difficult problem in itself. Results are influenced by changes in tool and work material, tool geometry, tool coating, cutting conditions, cutting fluids, etc. Understanding the interactions among these input parameters and the resulting fundamental process variables (Stage I) is an active research area. Obtaining a universal and quantitatively accurate chip formation model remains a major challenge. Thus, Stage II outcomes are the end-goal, while efforts on predicting Stage I variables still continue.

The physics-based analytical methods developed over decades provide a strong foundation for quantitative modelling of machining processes. Interest in numerical modelling (e.g., the Finite Element Modelling (FEM) and others) has continued to increase at rapid pace during the last four decades. Extensive reviews of analytical and numerical modelling efforts have been reported in cutting [75–79,183,184,276,311] or even in grinding [30]. Due to advances in computational techniques such as adaptive meshing and automatic remeshing, numerical hurdles in solution convergence for excessive mesh distortions have been largely overcome [38,83,186,191,216,229,259,294,300]. However, due to the extensive computational demands, it was not until the late 1990s that numerical methods became a useful and practical tool.

Despite significant recent advances, FEM still remains a "plug and play" technique for predicting some process output, depending on the assumed boundary conditions, including the friction. For example, it has been reported that FEM simulation of the same processes can produce different results [230]. For this reason, some sensitivity analyses have been conducted to study the effects of mesh definition, thermal conductivity, specific heat, flow stress models, values of the parameters in a flow stress model, friction model, coefficient of friction, and contact thermal properties [26,48,49,291]. However, in response to industrial demands, models have been developed for materials such as hardened

Table 1
Capabilities and limitations of modelling approaches.

	Analytical	Numerical	Empirical	Hybrid
Principle	Slip-line theory or minimum energy principle	Continuum mechanics using FEM, FDM & meshless FEM	Curve fitting of experimental data	Combines the strengths of other approaches
Capabilities	Predicts cutting forces, chip geometry, tool–chip contact length, average stresses, strains, strain-rates and temperatures	Predicts forces, chip geometry, stresses, strain, strain-rates and temperatures	Applicable to most machining operations for measurable process variables only	Provides meta-models for a family of models to be integrated
Limitations	Usually limited to 2-D analysis with single and multiple cutting edge, but some 3-D models exist	Material model, friction as input, computational limitations: e.g., meshing	Valid only for the range of experimentation	Limited to the strength of the base model: i.e., analytical, numerical, empirical, etc.
Advantages	Ability to develop fast practical tools	Opportunities to connect to industry-relevant parameters	Practical, fast and direct estimation of industry-relevant parameters	Improves the capabilities and accuracies of the base models
Disadvantages	Unique to each machining problem	Long computation time	Extensive experimentation, time-consuming and costly	Need for extensive data from experiments and/or simulations

steels, alloy steels, titanium and nickel-based super alloys [7,107,291,297]. Also, modelling of serrated and segmented chip formation, using damage criteria, has been proposed in many studies [34,38,110,228,270,295].

Table 1 shows the principal characteristics of the different modelling techniques. In the following sub-sections recent advances in analytical and numerical model development are summarized.

2.2. Analytical modelling

Slip-line field analysis has been an established analytical modelling method for investigating the chip formation process, typically under plane-strain, rigid-plastic conditions. Merchant's early shear plane model [198] was based on a single shear-plane with the velocity discontinuity across this plane being expressed by a velocity diagram (also known as hodograph). Subsequent work by Lee and Shaffer [170], and the mathematical foundations for constructing slip-lines from Hill [121] have been utilized by researchers since the 1950s through 1990s. Most notable among these include: strain-hardening slip-line model for orthogonal machining by Palmer and Oxley [232]; centred-fan slip-line model for machining with restricted contact tools by Johnson [142] and Usui and Hoshi [303]; Kudo's admissible and inadmissible slip-line models for machining [165]; strain-hardening solution with secondary deformation zone by Roth and Oxley [247]; Dewhurst's slip-line solutions for non-unique machining with curled chip formation [65]; and the subsequent extended curled chip formation model by Shi and Ramalingam [267].

More recently, Fang et al. [80] developed a universal slip-line model that incorporates all six previously presented slip-line models (Fig. 2). Fang and Jawahir [77] extended this rigid plastic universal slip-line model to include the effects of strain-rates and temperatures for machining of strain-hardening materials with conventional grooved tools. This model can predict cutting forces (F_c and F_t), chip up-curl radius (R_u), chip thickness (t_2), and chip back-flow angle (η_b), along with equivalent shear angle (ϕ_e), shear strain, strain-rate and temperature on the equivalent shear plane and restricted contact tool face as shown in Fig. 3 and Eqs. (1)–(10). Analytical predictions were experimentally validated [77–79].

Fang extended this prior research to utilize a rounded-edge tool [75,76]. Wang and Jawahir [314] used the universal slip-line model to determine the stresses on the tool's restricted rake face using the measured chip thickness and the chip up-curl radius (Fig. 4). The procedure adopted for accurately determining the actual state of

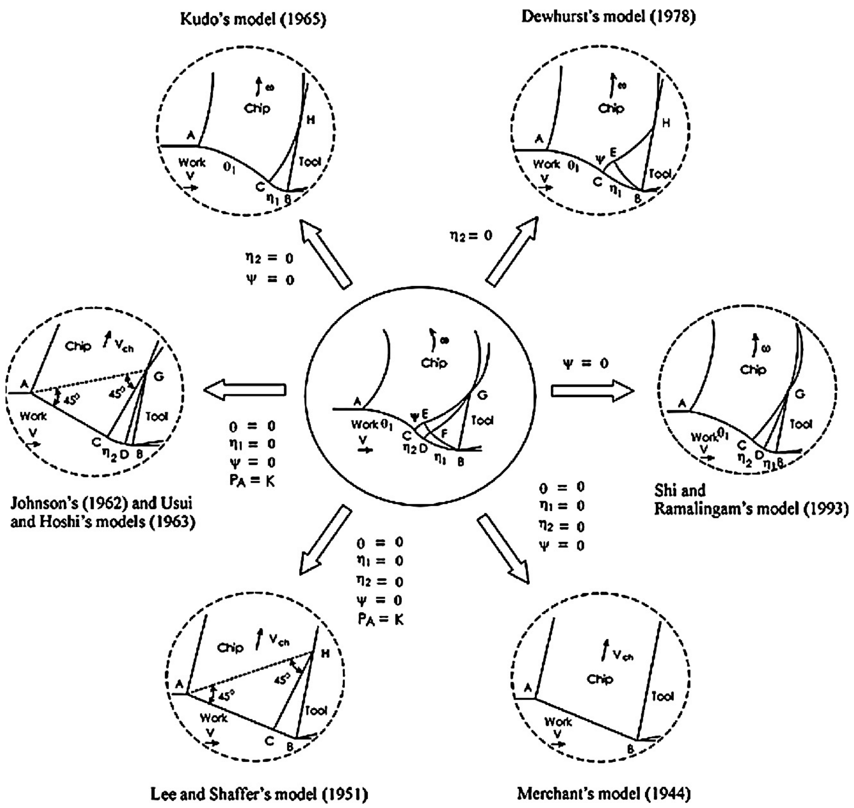

Fig. 2. Universal slip-line model and its transformation to six previous models (adapted from [80]).

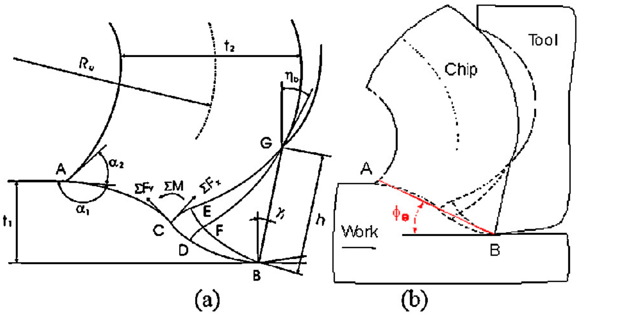

Fig. 3. (a) Universal slip-line model for machining with a grooved tool for predicting cutting forces, chip up-curl radius, chip thickness and chip back-flow angle, and (b) equivalent shear-plane model used for calculating the shear strain, strain-rate and temperature fields for strain-hardening materials [77].

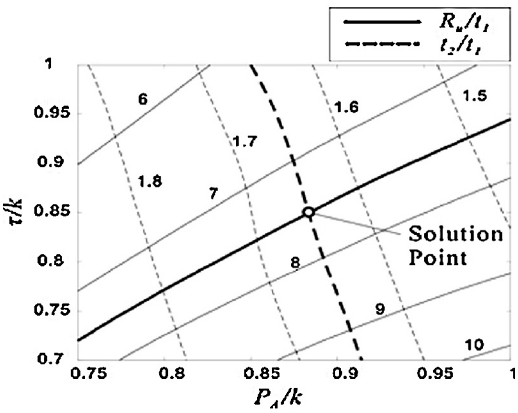

Fig. 4. Determination of the state of stresses using the universal slip-line model for machining with a grooved tool [314].

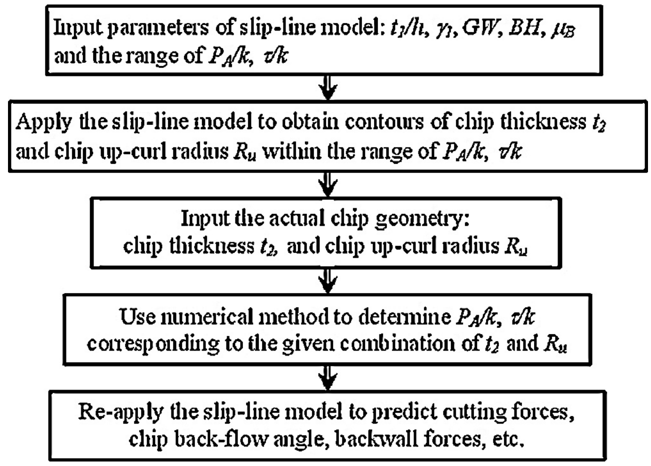

Fig. 5. The procedure adopted for determining the state of stresses and for calculating the variables for machining with a grooved tool [315].

stresses and for calculating the relevant variables such as cutting forces, chip back-flow angle, groove backwall forces, etc., is shown in Fig. 5.

Cutting force F:

$$\frac{F}{kt_1w} = \frac{F_{CA}}{kt_1w} + \frac{F_{CD}}{kt_1w} + \frac{F_{DB}}{kt_1w} \tag{1}$$

Chip up-curl radius:

$$P_\nu = \frac{1}{2}\sqrt{\left(\frac{\varsigma}{\omega}\right)^2 + \left(\frac{\rho}{\omega}\right)^2 + 2\cdot\frac{\varsigma}{\omega}\cdot\frac{\rho}{\omega}\cdot \chi O\sigma\alpha_1 + \frac{\varsigma_{\gamma'}}{2\omega}} \tag{2}$$

$$\alpha_1 = \pi - \gamma_1 - \varsigma + \theta + \psi - \eta_1 - \eta_2 \tag{3}$$

Chip thickness:

$$t_2 = 2\left(\frac{V_{g'}}{\omega} - R_u\right) \tag{4}$$

Chip back-flow angle:

$$\eta_b = arc\,tg\frac{V_{g'x}}{V_{g'y}} \tag{5}$$

Equivalent shear-plane angle:

$$\phi_e = \tan^{-1}\frac{\cos\gamma_1}{h_{ch}/h_c - \sin\gamma_1} \tag{6}$$

Shear strain:

$$\gamma_{AB} = \frac{\cos\gamma_1}{2\sin\varnothing_e\cdot\cos(\phi_e - \gamma_1)} \tag{7}$$

Shear strain-rate:

$$\dot{\gamma}_{AB} = \frac{C\cdot V_s\cdot\sin\phi_e}{h_c} \tag{8}$$

Temperature at AB:

$$T_{AB} = T_W + \eta\cdot\left[\frac{1-\beta}{\rho_s S}\cdot\frac{k_{AB}\cdot\cos\gamma_1}{\sin\phi_e\cdot\cos(\phi_e - \gamma_1)}\right] \tag{9}$$

Velocity-modified temperature:

$$T_{\text{mod}} = T\cdot\left[1 - \nu\cdot\log\left(\frac{\dot{\varepsilon}}{\dot{\varepsilon}_0}\right)\right] \tag{10}$$

Wang and Jawahir [315] subsequently developed a model for machining with a rounded cutting edge tool. This comprehensive model can predict the influence of the varying tool edge radius, tool geometry and groove parameters (tool restricted contact length, primary rake angle, groove width and back-wall height). Fig. 6 shows this extended slip-line model for machining with rounded

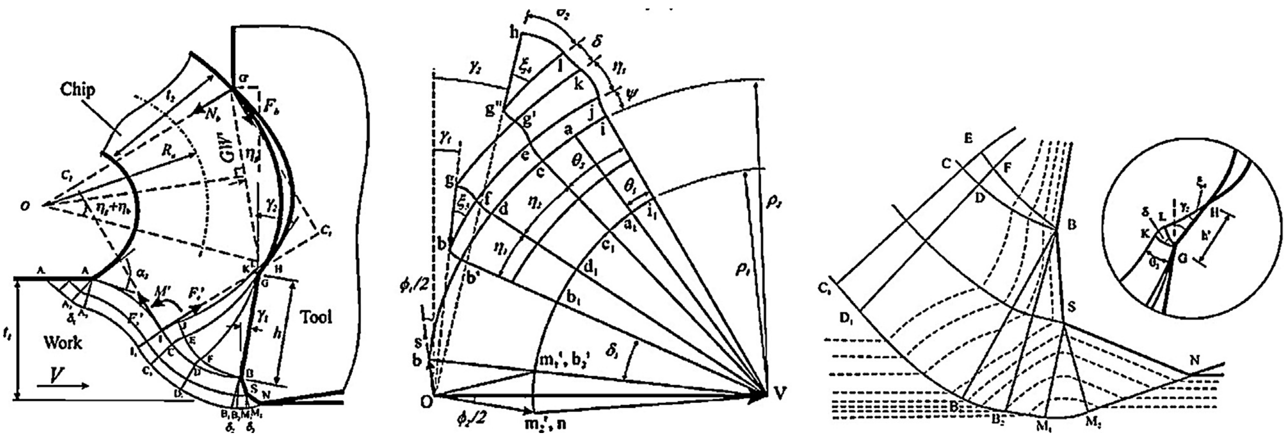

Fig. 6. Universal slip-line model for machining with a rounded cutting edged grooved tool including secondary rake and tool back-wall contacts, and the streamlines for material flow [315].

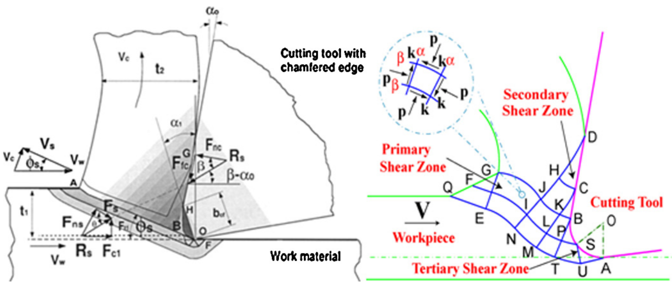

Fig. 7. Analytical chip formation model for (a) chamfered tool [243] and (b) micro-cutting [137].

cutting edged grooved tools, its hodograph and the additional slip-line field developed at the chip entry point of secondary face of the tool, along with the observed streamlines of material flow. Stresses, strains, strain-rates and temperatures for the entire slip-line field, as well as a floating stagnation point, *S* are predicted using this new model.

Using the Johnson-Cook material model and plastic contact with the tool, Karpat and Özel [148,149] developed slip-line models for round hone and chamfered tool geometry, with a dead-metal zone ahead of the tool. Ren and Altintas [243] used minimisation of total energy to analytically model machining with a chamfered tool and determined the shear angle in the primary deformation zone. Corresponding shear strain, strain-rate and flow stresses were used to predict the cutting forces and temperatures in deformation zones. More recently, Jin and Altintas [137] presented a slip-line field model for orthogonal micro-cutting process (Fig. 7).

2.3. Computational/numerical modelling

2.3.1. Finite Element Modelling: Lagrangian, Eulerian and ALE

There are two primary mathematical formulations of continuum-based FEM: Eulerian and Lagrangian. A Lagrangian mesh deforms in time with the material. An Eulerian mesh is fixed in space (control volume). In both analyses, either implicit or explicit time integration techniques can be utilized. A major disadvantage of the Eulerian formulation is the assumption of a steady-state mesh configuration. Thus, many researchers have adopted the computationally intensive Lagrangian approach. Schermann et al. [258] utilized the Lagrangian technique to study the interaction between cutting process and the dynamic behaviour of the machine tool.

The Arbitrary Lagrangian–Eulerian (ALE) technique combines the unique features of Lagrangian and Eulerian formulations, and adopts an explicit solution technique for fast convergence [9,210,228]. Arrazola and Özel [9] compared the ALE and explicit FE simulation techniques for the orthogonal cutting process (Fig. 8). While the ALE technique with Eulerian boundaries requires a predefined chip geometry assumption, chip formation is free of any geometrical assumption in ALE with Lagrangian boundary conditions. They also showed that these two similar approaches result in different chip formation.

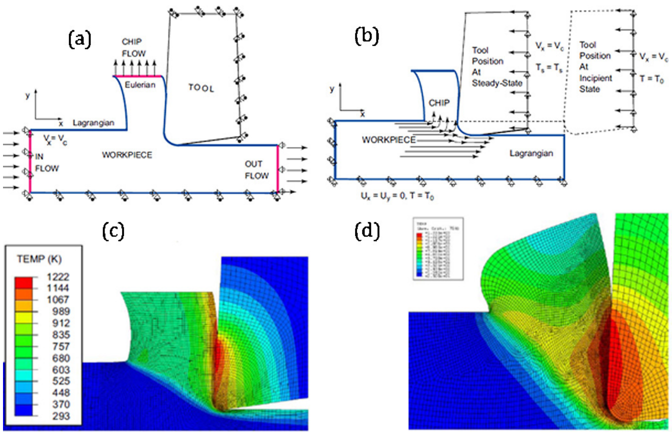

Fig. 8. FEM models for ALE formulation with (a) Eulerian and Lagrangian boundary conditions, and (b) pure Lagrangian boundary conditions, and the predicted temperature fields, respectively ((c) and (d)) [9].

2.3.2. Other numerical modelling techniques (BEM, FDM, SPH, etc.)

In other reported methods, based on conservation of mass, momentum and energy include meshless techniques [35,52], where material properties and state variables are approximated by their values at a set of discrete points, or particles. This avoids: (1) mesh distortion problems, (2) remeshing, (3) separation criteria, and (4) ad-hoc contact conditions with the creation of the new free surfaces and contact inherently managed. Calamaz et al. [35] used Smooth Particle Hydrodynamics (SPH) to model tool-wear in dry orthogonal cutting of a titanium alloy Ti6Al4V, and compared the results with experimental data. Uhlmann et al. [292] proposed a novel technique based on the finite pointset technique, and compared the results with FEM simulation for the same cutting conditions. In a similar approach, Al-Athel and Gadala [2] compared the Volume of Solid (VOS) technique with the ALE technique. Illoul and Lorong [127] used the Constrained Natural Element Method (CNEM) to simulate orthogonal and 3D oblique cutting processes.

2.3.3. Empirical models

Among the modelling approaches, empirical modelling is the simplest approach, and is widely used in the absence of other meaningful models. Empirical models often utilize statistical methods and they are only valid for the ranges of the experiments conducted, and not based on physics of the process. This technique is based on designing experiments for varying process inputs (e.g., cutting conditions, tool geometry, etc.) and measuring process performance such as cutting forces, surface roughness, tool-life, etc., and correlating them to the input conditions [46,223,230]. For this reason, this method utilizes heavy experimentation at different cutting conditions, cutting tools, and coolant/lubrication applications. Empirical models are largely outside the scope of this review paper.

2.3.4. Hybrid modelling

As explained in Section 1, combining some of the analytical, numerical, empirical or Artificial Intelligence (AI)-based methods, hybrid models can be created to predict industry-relevant process performance measures. In recent studies, AI-based methods such as neural networks, genetic algorithms, swarm intelligence, and other machine learning methods are being utilized to expand the capabilities of empirical models. There exists a vast amount of literature in this field [102,174,206,298]. However, this paper is concerned with mostly physics-based analytical and numerical modelling approaches, hence such models are not being reported here.

3. Model parameter identification

3.1. Modelling requirements

The success and reliability of modelling depends upon accurate mechanical (elastic constants, flow stress, friction, fracture stress/strain, etc.) and thermo-physical (density, thermal conductivity, heat capacity, etc.) data [224,226,251,270]. Thus, characterisation in the extreme conditions of machining: strains of 100–700%, strain-rates up to $10^6\ s^{-1}$, temperatures between 500 and 1400 °C, high heating rates close to $10^6\ °C\ s^{-1}$, and high pressures near 2–3 GPa, is needed. A realistic material model should also include strain-hardening and thermal softening due to dynamic recovery or recrystallisation. Therefore, other modelling approaches have been proposed, and flow stress data has been generated for machining of a range of commonly known work materials [34,47,111,150,225,264,269,270,316].

In order to overcome the limitations of existing test equipment, inverse methods have been proposed, but results are not always unique and may lack physical meaning [230]. For this reason, significant uncertainties due to input parameters alone still need to be overcome.

3.2. Input models and identification of input parameters

3.2.1. Material constitutive behaviour

3.2.1.1. Material models. In order to characterise material flow stress behaviour in metal cutting, researchers utilize flow stress expressions, some of which are summarized here.

Power law model: This model assumes a power law relation to determine flow stress as a function of strain, strain-rate and temperature (Eq. (11)).

$$\bar{\sigma} = \sigma_0 \left(\frac{\bar{\varepsilon}}{\bar{\varepsilon}_0}\right)^n \left(\frac{\dot{\bar{\varepsilon}}}{\dot{\bar{\varepsilon}}_0}\right)^m \left(\frac{T}{T_0}\right)^\tau \quad (11)$$

The experimentally determined exponents n, m, τ are the strain, strain-rate and temperature sensitivity, respectively.

Oxley's constitutive model: Oxley [222,223] showed that flow stress can be expressed as work-hardening behaviour (Eq. (12)) where σ_0 and n are written as functions of a velocity-modified temperature, in which strain-rate and temperature are combined into a single function (Eq. (13)),

$$\bar{\sigma} = \sigma_0 \bar{\varepsilon}^n \quad (12)$$

$$T_{MOD} = T\left[1 - \upsilon \log\left(\frac{\dot{\bar{\varepsilon}}}{\dot{\bar{\varepsilon}}_0}\right)\right] \quad (13)$$

where σ_0 is the strength coefficient, n is strain-hardening index, T is temperature, υ is a constant, $\bar{\varepsilon}$ is strain, and $\dot{\bar{\varepsilon}}$ is strain-rate. This model has been widely utilized in slip-line modelling of low and medium carbon steels [223].

Strain-path dependent model: A unique flow stress model that captures the effect of loading history was proposed by Maekawa et al. [187,188] (Eq. (14)),

$$\bar{\sigma} = A(10^{-3}\dot{\bar{\varepsilon}})^M e^{kT}(10^{-3}\dot{\bar{\varepsilon}})^{kT}\left[\int_{T,\bar{\varepsilon}\equiv(\dot{\bar{\varepsilon}})} e^{-kT/N}(10^{-3}\dot{\bar{\varepsilon}})^{-m/N} d\bar{\varepsilon}\right]^N \quad (14)$$

where k and m are constants and A, M, and N are functions of temperature. This model also captures the blue brittleness behaviour of low carbon steels where flow stress increases with temperature. The integral term accounts for the strain and temperature history. This model is difficult to implement in FEM software without modifications.

Johnson–Cook constitutive model: The Johnson–Cook model [140] is well-accepted, numerically robust and heavily utilized in modelling and simulation studies. This model describes flow stress as a product of strain, strain-rate and temperature dependent terms as given in Eq. (15),

$$\bar{\sigma} = [A + B(\bar{\varepsilon})^n]\left[1 + C\ln\left(\frac{\dot{\bar{\varepsilon}}}{\dot{\bar{\varepsilon}}_0}\right)\right]\left[1 - \left(\frac{T - T_0}{T_m - T_0}\right)^m\right] \quad (15)$$

where the parameter A is the initial yield strength of the material at room temperature, $\dot{\bar{\varepsilon}}$ is the equivalent plastic strain-rate normalized with a reference strain-rate $\dot{\bar{\varepsilon}}$, T_0 is the room temperature, T_m is the melting temperature of the material, and B, C, n and m are model parameters. The parameters n, m and C are the strain-hardening exponent, thermal softening exponent and strain-rate sensitivity, respectively. The Johnson-Cook model neglects the coupling of strain, strain-rate and temperature known to occur in real materials. Thus, modified versions have been suggested by Calamaz et al. [34,36] and Sima and Özel [270] to include novel multiplicative strain and temperature dependent terms as shown in Eq. (16).

$$\sigma = [A + B(\bar{\varepsilon})^n]\left[1 + C\ln\frac{\dot{\bar{\varepsilon}}}{\dot{\bar{\varepsilon}}_0}\right]\left[1 - \left(\frac{T - T_0}{T_m - T_0}\right)^m\right]\left[D + (1 - D)\left[\tanh\left(\frac{1}{(\bar{\varepsilon} + p)^r}\right)\right]^s\right] \quad (16)$$

In this model, the parameters D and p are dependent on workpiece temperature as follows:

$$D = 1 - \left(\frac{T}{T_m}\right)^d, \quad p = \left(\frac{T}{T_m}\right)^b$$

Guo and Liu [107] proposed tensile tests at elevated temperatures coupled with a velocity-modified temperature to approximate material properties of hardened *AISI 52100* steel. An integrated approach combining compression tests and cutting experiments has been developed [109], and this reconfirms that compression testing may be more suitable for modelling the material behaviour in metal cutting. Later, Guo et al. [111] introduced an internal state variable (ISV) model for FE modelling of chip formation in *AISI 52100* steel and *Ti6Al4V* alloy. They predicted continuous, saw-tooth, and discontinuous chip formation, temperatures, and other process variables for a range of cutting conditions. Several researchers have focused on new material models to explain serrated chip formation in the machining of *Ti6Al4V* alloy [150]. Other researchers [255,295] modified Eq. (11) to capture material strength variation due to hardness modification during machining.

3.2.1.2. Identification of constitutive law coefficients. The parameters of the constitutive laws are obtained through the testing of material samples in experimental high speed equipment such as Taylor's impact tests or Hopkinson's compression and shear devices. These devices can provide working conditions close to machining, but they are not adequate for all situations. One possibility to improve the material test conditions is to use a sample with a special shape [43] to reach shear strains nearing 4, strain-rates of $2 \times 10^4\ s^{-1}$ and temperatures of 300 °C, but these values are still far from those encountered in machining. Burns et al. [33] developed a Split Hopkinson Pressure Bar (SHPB) bar with rapid heating and found that even the rate of heating can affect the results. Identification of other parameters encountered such as friction coefficient, heat partition factor, etc. presented similar difficulties and researchers have worked to improve testing methods [57,205,323].

Chandrasekaran et al. [41] used the split Hopkinson bar in order to determine an initial material constitutive law. Subsequently, material flow stress valid in the context of chip formation was computed from an analytical model based on results from orthogonal milling tests. Shi et al. [265,266] developed a novel methodology to overcome SHPB limitations. This methodology combines finite element simulations with the theory of plasticity and heat transfer, as well as the orthogonal metal cutting mechanics, in order to obtain the primary shear zone temperature, strain and stress distributions.

Özel and Altan [224] used the FE simulation of chip formation process together with orthogonal cutting test measurements, to generate flow stress and friction data for *AISI P20* mould steel. Warnecke and Oh [316] introduced a procedure for high strain-rate flow stress determination to include blue brittleness and phase change effects in low-to-medium carbon and alloy steels.

Shatla et al. [256] used Oxley's parallel-sided shear zone theory [223] to model orthogonal milling. They modified the Johnson-Cook material model by inverse analysis and demonstrated applications in numerous materials. Özel and Zeren [225] also utilized Oxley's analysis to calculate flow stress at high strain-rates. By combining data from orthogonal machining with SHPB experiments, a Johnson–Cook material model was obtained for

AISI 1045 steel, *Al 6082-T6* aluminium, and titanium alloy *Ti6Al4V*. More recently, Shrot and Bäker [269] used experimental measurements of chip morphology and cutting force in a Levenberg–Marquardt search algorithm to identify the Johnson Cook parameters for high speed machining.

3.2.2. Friction

3.2.2.1. Friction models. Since the rate of tool-wear heavily depends on the frictional conditions at the tool–chip interface, accurate modelling is of critical practical importance [226]. Friction under very extreme conditions is prevalent at the tool–chip interface (1-2 GPa, 500–100 °C) [324]. In spite of this, in early analyses of machining, Coulomb-type models were used, where the frictional stresses (τ_f) on the tool rake face were assumed to be proportional to the normal stresses (σ_n) with a coefficient of friction (μ). In conventional machining at low cutting speeds, the Coulomb model is found to be effective for describing the frictional conditions at the tool flank face, but not at the rake face.

Zorev [327] proposed a more realistic representation in a stick-slip friction law based on normal and shear stress distributions. This is represented mathematically as a sticking region, where the flow stress is equal to the plastic yield, and a sliding region, where a Coulomb model is adopted (Eq. (17)).

$$\tau_f(x) = \begin{cases} \tau_p & 0 < x \le l_p \\ \mu\sigma_n & l_p < x \le l_c \end{cases} \tag{17}$$

Usui and Shirakashi [305] derived an empirical equation to describe the stress characteristics in a friction model at the tool–chip interface (Eq. (18)).

$$\tau_f = k[1 - e^{-(\mu\sigma_n/k)}] \tag{18}$$

where k is the shear flow stress and μ is a friction coefficient obtained from experiments. This equation allows a smooth transition between a plastic and a sliding zone. Dirikolu et al. [66] made a further modifications to this model by multiplying k by a friction factor m, where $0 < m < 1$, and by introducing an exponent n (Eq. (19)).

$$\tau_f = mk[1 - e^{-(\mu\sigma_n/mk)^n}]^{1/n} \tag{19}$$

In recent years, Zemzemi et al. [323] have proposed the use of friction models depending on sliding velocity (Eq. (20)).

$$\mu = A(V_s)^{-B} \tag{20}$$

3.2.2.2. Identification of friction law parameters. In the case of tool–chip contact, Puls et al. [239] recently proposed an orthogonal cutting operation on a disc with an extreme negative rake angle to identify the friction coefficient in conditions closer to the cutting process. This solution however, does not provide information about the heat partition coefficient. Zemzemi et al. [323] proposed a method for fast identification of tool-work material friction and heat partition models, based on a specially designed tribometer, and was able to simulate wide ranges of contact pressure and sliding velocity, similar to those occurring along the tool-work material interface in machining. This method is presented and applied to a wide spectrum of work materials and lubrication conditions. Combined with an analytical post-treatment, this set-up may also provide a model of frictional behaviour that may significantly improve thermal aspects in machining simulations [72,128,248]. In this way, Claudin et al. [57] showed that straight oil decreased the friction coefficient at low cutting speeds (<150 m/min) when machining steel.

3.2.3. Thermal parameters and variables

3.2.3.1. Thermal models. To accurately model the machining process, thermal and tribological properties of work and tool materials are critical. These temperature-dependent properties include specific heat or heat capacity, c_p (T) in [N mm^{-2} °C^{-1}], modulus of elasticity, E (T) in [MPa], thermal expansion, α (T) in [mm mm^{-1} °C^{-1}], and thermal conductivity, λ (T) in [W m^{-1} °C^{-1}].

In addition, the global heat transfer coefficient h must be adequately known. Attanasio et al. [10] proposed a model for the heat transfer depending on contact pressure.

As in friction, Zenzemi et al. [323] have proposed using heat partition factor models depending on the sliding velocity (Eq. (21)):

$$\alpha = A(V_s)^{-C} \tag{21}$$

3.2.3.2. Identification of the thermal parameters. Schmidt and Roubik [275] showed the dependence of heat partitioning on the feed rate and cutting speed using an orthogonal cutting model of drilling steel. Yen et al. [322] and Fleischer et al. [92] attempted to tune the global heat transfer coefficient to improve the convergence of finite element simulations. Childs et al. [45] tried to evaluate the effect of coolant on the heat transfer when machining mild steel using a HSS tool, at cutting speeds ranging from 33 to 61 m/min. However, Claudin et al. [57] did not observe any influence of the coolant on the heat transfer to the tool. Fleischer et al. [91] determined the heat flux transferred to the workpiece in dry drilling and showed that an increase in the heat flux is mainly caused by reducing the feed per tooth. In contrast, for greater cutting speeds, the calculated heat flux transferred to the workpiece decreased. Recently, Sölter and Gulpak [273] proposed an empirical model for the heat flux in dry milling of steel with particular attention to the distribution of the heat flux at the tool workpiece engagement zone. Pabst et al. [231] and Solter et al. [273] determined the heat flux transferred into the workpiece in dry milling.

Attanasio et al. [11] proposed a model for the heat transfer coefficient [kWm^{-2} K^{-1}] obtained from experiments and FE simulations when an *AISI 1045* workpiece was machined with a coated cemented carbide tool, ISO P40 (Eq. (22)).

$$h = 17529 - 34.752 \cdot p - 1.019 \cdot T + 0.01756 \cdot p^2 + 0.000783 \cdot T^2 \tag{22}$$

where p and T are the average normal pressure on the rake face in [MPa] and the average temperature along the contact length in [°C], respectively. Moreover, it was demonstrated that the global heat transfer coefficient increases if both the pressure and the temperature at the interface increase.

Courbon et al. [59] analysed the thermal contact between *AISI 1045* steel and *TiN* coated carbide tool and showed that thermal contact resistance did not affect average cutting forces and chip geometry. However, they found that it has a considerable influence on temperature amplitudes and distributions.

3.2.4. Damage

Ductile failure or fracture occurs during chip separation and formation. Typically this can only be directly captured by FEM. Some researchers directly utilize ductile fracture models while others use frequent remeshing to indirectly simulate separation. Since segmented and discontinuous chip formation in hard materials involves separation of material, Ceretti et al. further employed fracture or damage criteria to predict this behaviour [38,39].

Fracture models utilized for chip separation are often non-path-dependent failure criteria involving critical strain, critical strain-to-fracture and maximum shear stress. Ductile failure or fracture occurs when the accumulated plastic strain reaches a critical value. The critical strain method is the most commonly used method to study the crack initiation and propagation ahead of the tool edge [216,217]. Another model based on strain-to-fracture, which was proposed by Johnson and Cook [141], is given below (Eq. (23)), and this model has been utilized in simulation of segmented chip formation [110].

$$\bar{\varepsilon}_f = [D_1 + D_2 \exp(D_3\sigma^*)][1 + D_4 \ln \dot{\varepsilon}^*][1 + D_5 \ln T^*] \tag{23}$$

where σ^*, $\dot{\varepsilon}^*$, T^* are the stress, strain-rate and temperature, respectively and D_1, D_2, D_3, D_4 and D_5 are material-dependent fracture constants.

Most criteria for accumulating damage during chip formation are based on increasing strain and stress which can be easily implemented in FEM. These models include Cockroft and Latham [58], Brozzo et al. [31] and McCintock [185]. The critical damage value is considered as a material constant even though it is affected by the process conditions. Therefore, damage criteria and critical damage value (D_{cr}) are considered as additional process inputs to models.

According to Cockroft and Latham (C–L) damage criterion, the damage occurs when the accumulated stress state, D, over the plastic strain reaches a critical damage value (D_{cr}), as given in (Eq. (24)), where $\bar{\varepsilon}_f$ is the effective fracture strain and σ_1 is the maximum principal stress.

$$D = \int_0^{\bar{\varepsilon}_f} \sigma_1 d\bar{\varepsilon} \tag{24}$$

3.2.5. Wear

3.2.5.1. Wear models. During the last few decades, numerous researchers have investigated and attempted to model and predict tool-wear [64,71,153,154,166,195,204,281,306]. In order to predict tool-wear, models that describe the rate of local volume loss on the tool contact face (rake or flank face) per unit area per unit time are required. This type of model would be needed to reflect the knowledge of wear mechanisms associated with the tool and workpiece materials and the range of cutting conditions used. These models can be used in concert with other techniques to estimate tool-wear progression [319].

Usui and co-workers [153,154,304] derived a wear rate model based on the equation of adhesive wear (Eq. (25)), which involves temperature, T, normal stress, σ_n, sliding velocity at the contact surface, v_s, and two constants A and B. Their results show that both the flank and crater wear rates have the same functional form.

$$\frac{\partial W}{\partial t} = A v_s \sigma_n \exp\left(-\frac{B}{T}\right) \tag{25}$$

Takeyama and Murata [281] derived a fundamental wear rate equation (Eq. (26)) by considering abrasive wear $G(V, f)$, which is proportional to cutting distance, and is a function of the activation energy of the diffusion process containing both entropy and enthalpy terms, E.

$$\frac{\partial W}{\partial t} = G(V, f) + D \exp\left(-\frac{E}{RT}\right) \tag{26}$$

where R is the gas constant, T is the local temperature, and D is a material constant.

3.2.5.2. Identification of wear model parameters. Mathew [195] studied tool-wear in carbide tools when machining carbon steels, and confirmed previous results obtained by Takeyama and Murata [281]. Also, Childs et al. [46] obtained similar results using an electron probe micro-analyzer (EPMA). Molinari and Nouari [204] modelled crater wear by diffusion in high speed cutting. In their model, sliding velocity governed mass transfer via material flow, and influenced the contact temperature. Since diffusion is governed by the mean contact temperature, crater wear depth was connected causally to machining parameters.

Klocke and Frank [156] proposed a regression analysis to determine the material constants A and B in Usui's wear model, and found that the proposed abrasive model can predict both the flank and the crater wear. Their findings were in agreement with those found some years early by Kitagawa et al. [153,154], where crater and flank wear experimental data points can be interpolated by only one line in a semi-logarithmic chart.

4. Prediction of fundamental variables

As stated in Section 1, prediction of industry-relevant parameters requires prediction of fundamental process variables as a prerequisite. It is worth mentioning that except for cutting forces, power or torque, in all the remaining fundamental variables empirical measurements are made commonly in 2D, e.g., strain, strain rate, temperature, etc. Other variables such as stresses in the workpiece, or in the cutting tool, cannot be even empirically measured during cutting process. For instance, residual stresses on the workpiece surface and subsurface should be measured subsequently. However, FEM can provide some useful data about these variables in 2D and 3D machining.

4.1. Cutting forces

Force modelling techniques include empirical, mechanistic, analytical, numerical and hybrid models. Reviews show numerous modelling efforts [7,19,95,143,164,223,277,289,327]. Mechanistic force modelling is among the most commonly known modelling procedures currently used in practical applications. In these models (Eq. (27)), the forces are formulated as functions of specific cutting forces (K_i where i = tangential, radial, axial), and the cutting forces for very complex tools can be generated by integrating the above mentioned specific cutting forces over the entire tool surface.

$$F_i = K_i bh + K_{ie} \tag{27}$$

Cutting tests are required to calibrate the models by generating the coefficients for different work/tool material combinations, tool coatings and geometries. However, once the models are calibrated, force prediction error within the calibration range becomes accurate with less than 5% uncertainty, and can be used in virtual machining applications [4].

Analytical models based on slip-line field analysis can also predict machining forces [77,315]. Similarly, analytical thermo-mechanical cutting process models that directly utilize material constitutive models (Section 3.2.1) can predict forces and the temperature fields. Budak and Ozlu [32] applied these models to oblique cutting and integrated them with milling mechanics to obtain force predictions in ball-end milling. Hybrid models, that use FEM to obtain specific cutting forces for mechanistic milling models, have also been introduced [138,312]. Based on an energy approach (chip flow angle is determined to minimize the cutting energy) Matsumura and Usui [196] presented a force model for the complex tools such as the ball and the roughing end mills. Tamura et al. [282] estimated the drilling forces when machining *Ti64* alloy. Subsequently, the cutting force prediction model was applied to contour milling operations [197]. Moufki et al. [207,208] developed a predictive model of oblique cutting. This model has been applied to ball-end milling in [87–89] where the engaged part of cutting edge is divided into small differential oblique cutting edges segments. For turning process model, Molinari & Moufki [205,209] proposed a new approach by considering the fact that the local chip flow, for a cutting edge element, is imposed by the global chip movement. Bahi et al. [16,17] considered the sliding and sticking zones at the tool-chip interface by using a hybrid analytical-FE (finite elements) model. This approach permits to take into account the relationship between the local friction coefficient, in the sliding zone, the apparent or global friction coefficient and the thermo-mechanical coupling.

4.2. Temperature fields and heat partition

Temperature prediction is of critical importance because it governs thermally activated wear mechanisms. Komanduri and Hou [161] give a comprehensive review of the state-of-the art in empirical methods. The prediction of cutting temperature fields and heat partition still presents a major challenge. Since the measurement of temperatures in machining operations is difficult, validation of any model is a challenge. A true predictive model should predict the temperature fields taking into account the the empirical uncertainty [63].

Temperature fields can be obtained by using various types of models; (i) analytical thermal modelling, (ii) combined slip-line and analytical thermal modelling, (iii) finite difference methods and (iv) FEM.

Analytical thermal modelling utilizes a moving band heat input that is a large fraction of the mechanical work input to predict a temperature field (Fig. 9). The pioneering work by Komanduri and his colleagues [161–163] was adopted, and subsequently improved by many researchers: [124,125,146,147,211].

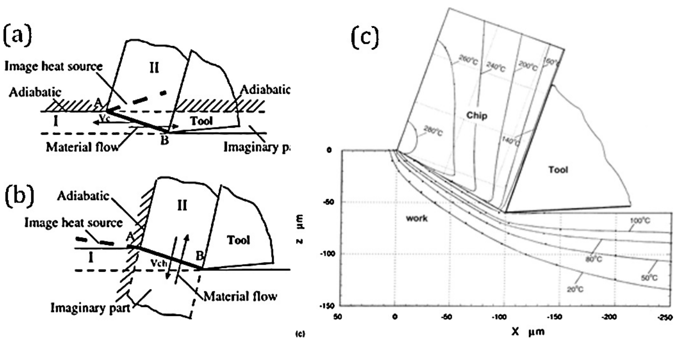

Fig. 9. Analytical model for thermal analysis of (a) work and (b) chip; (c) predicted temperature field (AISI 1113 steel, v_c = 2.32 m/s, t_c = 0.06 mm) [161].

Lazoglu and Altintas [168] developed a finite difference method to predict tool and chip temperature fields in orthogonal cutting. Karpat and Özel [149] combined slip-line and analytical thermal models to predict temperature fields in high-speed machining of *AISI 4340* with a chamfered tool (Fig. 10).

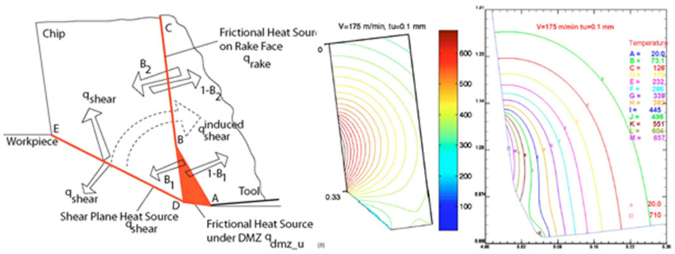

Fig. 10. Heat sources in thermal modelling of orthogonal cutting of *AISI 4340* steel with a chamfered *PCBN* tool and predicted temperature field v_c = 175 m/min, a_p = 0.1 mm (temperatures are in °C) [149].

A special variant of the finite difference method called the method of elementary balances (MEB) was used by Grzesik et al. [100,101] in order to predict temperature distributions along the rake and flank faces as well inside the tool body. They analysed temperature values for uncoated ISO *P20* carbide tools and coated tools when machining C45 carbon steel at the cutting speed range of 72–145 m/min [100], and obtained reasonably good correlation (6%) compared to thermocouple measurements. It was also revealed [103] that the heat source shape influences the temperature distribution along the tool–chip interface. In particular, the trapezoidal heat source gives better agreement between computed and measured average interface temperature values.

Usui et al. [304] predicted the cutting temperature using the finite difference method by utilising the predicted cutting force based on the energy approach. Subsequently, the crater wear progression was predicted with stresses and temperatures at the rake face.

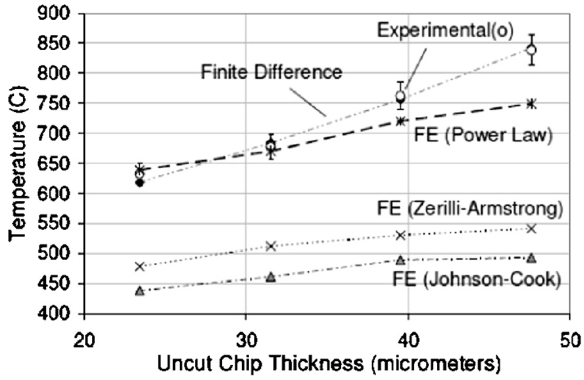

Fig. 11. Comparison of experimental and modelled temperatures [62].

Several researchers have used FE simulations to predict temperatures for various cutting conditions, tool material, coating and edge geometry [221,227]. Davies et al. [62] compared the measured temperature fields to the FEM predicted ones (Fig. 11). They concluded that there could be large discrepancies in these methods due to (1) material data that is not suitable for the deformation regimes and/or heating rates encountered in machining; (2) errors in the adopted method of modelling of machining; (3) errors in the experimental system not accounted for by the uncertainty analysis [8] (Fig. 12); (4) failure of the FE simulations to reach equilibrium temperature; and/or (5) failure to account for preheating of the workpiece due to previous cutting.

4.3. Stresses, strains and strain-rates

To date stress fields (Von Mises and principal) have only being obtained using FEM. Predicted stresses are highly sensitive to thermo-mechanical loading and tool–workpiece contact conditions, and therefore most accurate predictions can be made by using elastic-viscoplastic work assumptions. More discussion on stresses is presented on predictions of residual stress in Section 5.2.1. Strain fields can be obtained using FEM (e.g., in Fig. 13), and

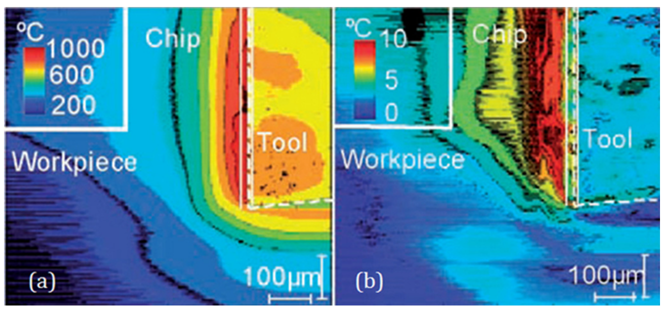

Fig. 12. Thermal maps for (a) standard temperature and (b) uncertainty [8] (v_c = 400 m/min and a_p = 0.2 mm. *AISI 4140* steel).

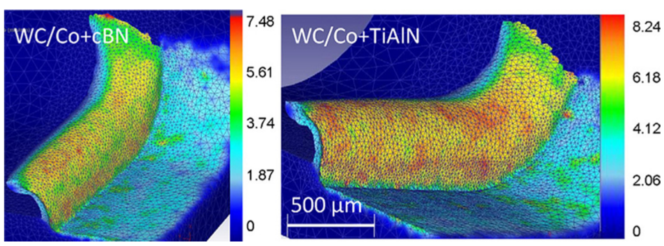

Fig. 13. Predicted strain fields using FE simulations in machining of *Ti6Al4 V* alloy using coated tools (v_c = 100 m/min, a_p = 2 mm, f = 0.1 mm/rev, ε_{max} = 8.24 for cBN and ε_{max} = 7.37 for *TiAlN* coated tools) [227].

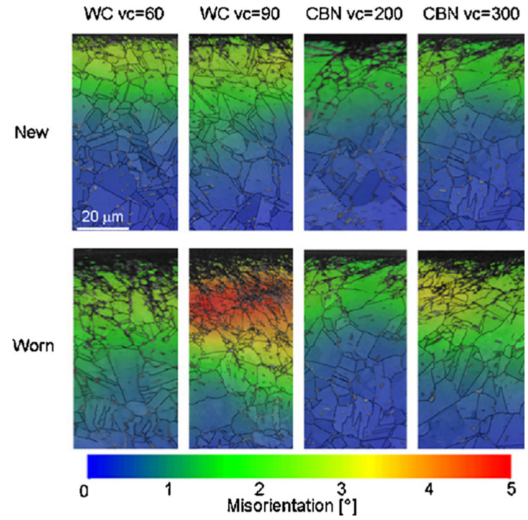

Fig. 14. Strain contour maps obtained from electron beam scattering diffraction measurements [212].

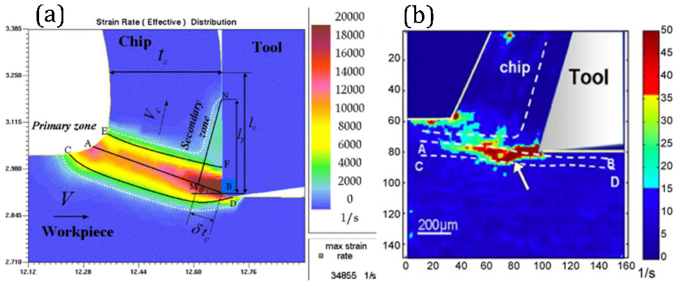

Fig. 15. Deformation zones and strain-rate field in orthogonal cutting: (a) FE simulation of chip formation in *AISI P20* steel and carbide tool [225], (b) measured shear strain-rate field using the PIV in machined copper [42].

have recently been compared with diffraction-based measurements (Fig. 14) [212]. The distribution of effective strain-rates in the shear zone can be obtained using FE simulations (Fig. 15). Validation of strain-rate predictions remains a major experimental challenge [42].

5. Predictive performance model development

5.1. Chip formation, chip flow, chip breaking and chip morphology

The investigation of chip formation, chip flow and chip breaking include analytical, numerical and experimental studies. Previous two CIRP keynote papers have covered this topic extensively [130,309]. Significant modelling efforts have been reported since these major CIRP initiatives. These include modelling of 2D cyclic chip formation and chip breaking [93], analytical modelling of 3D chip curl and breaking [96,97], slip-line modelling of 2D chip curl [80,81,132], analytical modelling of orthogonal chip breaking process [73], and FEM analysis of 2D chip breaking process [325]. A hybrid algorithm was developed and presented for predicting chip-form and chip breaking in machining operations [74]. Subsequently, an advanced AI-based predictive system was developed for predicting chip breaking in turning operations involving automatic computerized generation of chip charts, which significantly reduced the traditional experimental chip chart developing process from a few hours to just a few seconds [133]. This method also considered the effect of complex chip-groove geometry on chip flow and chip curl, and was successfully used in automotive powertrain machining operations. Later, this method was fully implemented within an optimisation system to predict chip breaking in machining processes [135]. More recently, Balaji et al. [20] presented a summary of modelling efforts on chip-formation, including chip breaking.

Davies and Burns [61] developed an analytical nonlinear dynamic approach for predicting the transition from continuous to shear – localized chip formation. Molinari et al. [203] analysed the evolution of the cutting forces in terms of cutting speed for *Ti6Al4V* alloy. They proposed a model to determine shear band width and frequency. Recently Pawade et al. [235] presented an analytical model that predicts specific shearing energy of *Inconel 718* in the shear zone. Perhaps more than in any other area, the use of numerical techniques such as FEM for studying the chip formation process has accelerated sharply since previous CIRP keynotes. Even as late as the early 1990s, continuous straight chip formation had been the primary focus of numerical modelling due to: (1) limited computer capacity, (2) limitation of stress update, (3) contact algorithms, (4) convergence problems, and even (5) the lack of fundamental understanding of chip separation/segmentation mechanisms. In the early 1990s, Ueda and Manabe [290] modelled 2D and 3D chip formation processes. Their procedure was implemented in a rigid-plastic FEM with a novel remeshing algorithm applied only in the vicinity of the cutting tool edge. In the middle of 1990s, Obikawa and Usui [217] and Obikawa et al. [218] used a fracture criterion and geometrical chip separation for producing saw-tooth type chip generation. However, the use of a pre-defined cutting line made their simulation and the type of chip different to that which occurs in real machining. The progress of powerful remeshing algorithms [38,155,316] made it possible to simulate chip formation without pre-defined separation lines. Also, the development of new finite element formulations such as those proposed by Hashemi et al. [116], Marusich and Ortiz [191], the ALE technique [210], and the proposition of robust fracture criteria, made possible the numerical simulation of segmented/discontinuous and serrated chip formation [39,40,155,268] (Fig. 16). By using damage criteria based on accumulated plastic strain, serrated and segmented chip formation has been achieved [18,36,110,268,272,297]. Various damage and separation criteria were reused in these studies as described in the review proposed by Vaz et al. [311]. Indeed, studies of discontinuous chip formation have become a very active research area, which now includes both the effect of hydrostatic stress and the damage criteria [23,178,179,216,228,295]. Although 2D FEM has great capacity for understanding the fundamental process mechanics in cutting [48–51], a 3D FEM enabled by more powerful computers provides a more realistic representation of practical cutting operations.

Guo and Dornfeld [105] and Guo and Liu [106] simulated incipient cutting in drilling and turning, respectively. Other advances have been made in studying oblique cutting [13,24,27,84,152] and in high speed machining [34,69,190,313] (Fig. 17).

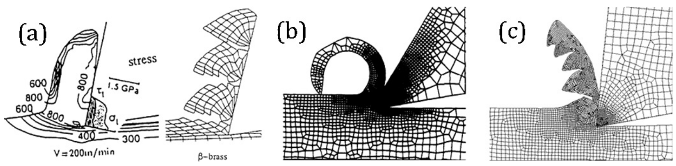

Fig. 16. (a) Saw-tooth chip produced using fracture criterion and geometrical chip separation [218]; (b) continuous [38]; and (c) discontinuous chip morphology [39] from remeshing, with no pre-defined cutting line.

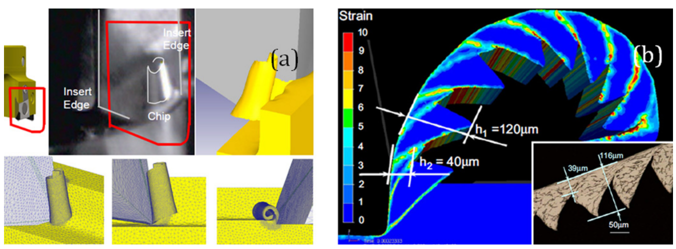

Fig. 17. (a) Comparison of CAD solid model of cutter, image from high speed video, and FEM simulation results for machining of *AA 7075* alloy [84]. (b) Predicted and experimental chips of *Ti6Al4V* alloy at cutting speed of 180 m/min and a feed of 0.1 mm [34].

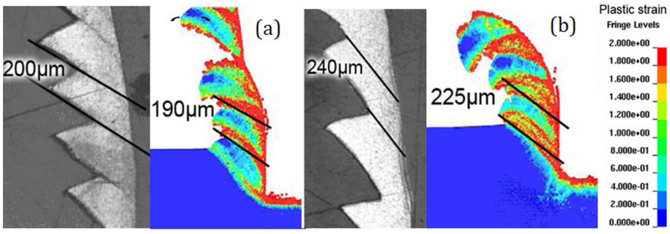

Fig. 18. Experimental and numerical chip formation obtained with SPH method using (a) new tool and (b) worn tool geometry [35].

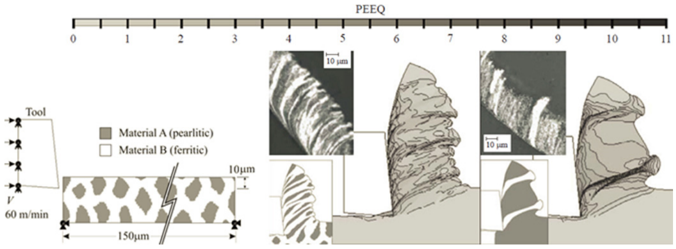

Fig. 21. Equivalent plastic strain during chip formation from the refined FE cutting model [272].

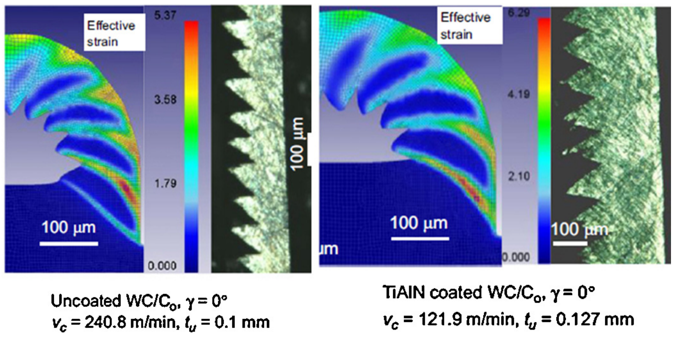

Fig. 19. Comparison of simulated serrated chip formation and captured chip images [270].

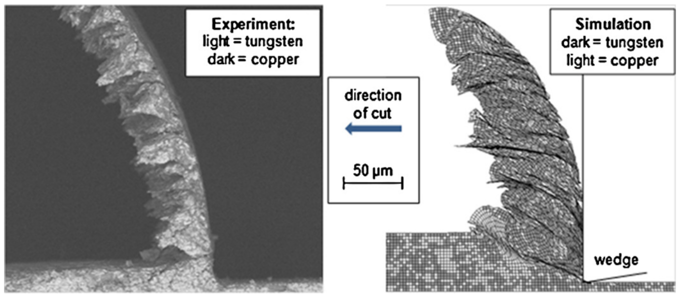

Fig. 22. Comparison of experimental and simulated tungsten-copper-chip roots [98].

Calamaz et al. [35] used the SPH method in LS–DYNA software to obtain serrated chip formation for orthogonal cutting of titanium alloy *Ti6Al4V*, and compared the results to experimental data (Fig. 18). Also, the formation of shear localized chips in the cutting of titanium alloy *Ti6Al4V* has been simulated by using elastic-viscoplastic FEM with a modified material model using commercial FEM programmes (Fig. 19) [270].

The role of friction on chip-form during orthogonal cutting was emphasised by Leopold and Wohlgemuth [173] (Fig. 20a). Also, Rizzuti [245] showed that different flow stress models of *AISI 1045* steel produced markedly different results (Fig. 20b).

Application of FEM to aerospace materials, such as nickel-based alloys and fibre reinforced plastics has become an active area of research [182,228,291]. Quite recently, Santiuste et al. [250] investigated the chip formation mechanisms of glass and carbon fibre reinforced polymer (GFRP and CFRP).

Over the past five years, the effect of material microstructure has been studied through FEM. It has been found that the incorporation of the material microstructure yields more realistic chip formation, and unlike homogeneous models, it allows for the analysis of surface defects in machining [56,271]. The effect of grain size and grain orientation is also important in microcutting [272]. The incorporation of microstructure effects is difficult, but would more accurately reflect the workpiece material stress-strain behaviour in the primary shear zone (Fig. 21).

A similar methodology was applied by Graf von der Schulenburg and Uhlmann [98] for modelling chip formation when tungsten-copper-composite was machined. Fig. 22 shows the comparison of an experimental and a simulated chip. The simulated chip roots show the same phenomena, which have been previously observed with the chip roots in the experiments, even for different tungsten-copper specifications such as various tungsten particle diameter and tungsten/copper ratio.

Schulze and his co-workers [15,123,317] fully described the size effects in chip formation with geometrically defined cutting edges. From this, extended similarity relations were derived to deepen the understanding of size effects for machining processes and to strengthen the transfer of miniaturized machining processes to technical application. The importance of geometrical and physical size effects were also emphasized respectively by Guo and Wen [110] and Guo and Anurag [112] who simulated saw-tooth and discontinuous chip formation in hardened *AISI 52100* steel using a shear failure model (Fig. 23). Guo et Anurag also used an internal state variable (ISV) based plasticity model to handle the complex loading history explaining the Bauschinger effect, recovery, and adiabatic effects in polycrystalline materials with random microstructures. A recent CIRP keynote paper by Vollertsen et al. [310] presents a comprehensive review and historical perspective of the size effects in machining.

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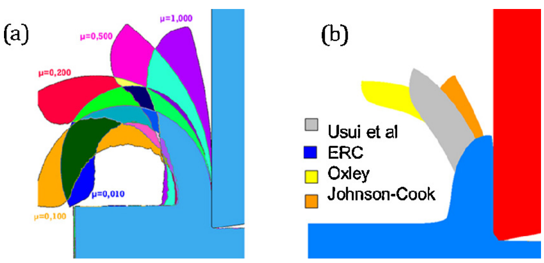


Fig. 20. Influence of (a) friction [173] and (b) material modelling [245] of the chip geometry (v_c = 150 m/min), f = 0.1 mm/rev).

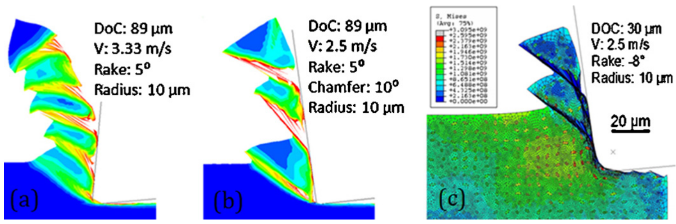


Fig. 23. Metal cutting simulation with geometrical size effect [110], showing (a) saw-tooth chip formation, (b) discontinuous chip formation and (c) physical size effect [112] with random microstructures.

5.2. Surface integrity

A major goal for developing machining models is the prediction of surface and sub-surface integrity due to the fact that it relates directly to the quality, performance and life of machined components. Results of a CIRP-sponsored comprehensive benchmark evaluation were reported in the recent CIRP keynote paper by Jawahir et al. [136]. Residual stresses, surface hardness and microstructural changes are among the most commonly evaluated surface integrity parameters.

5.2.1. Residual stresses

For the last two decades, a greater emphasis has been placed on developing predictive models for machining-induced residual stress profiles because of their direct relevance to fatigue life, fracture behaviour and wear/corrosion resistance of machined components. The need for investigating the effects of cutting conditions, tool geometry, cooling conditions, and more recently, microstructural changes and size effect on residual stresses in the machined surface layer, has emerged with a significant research effort being made by the international research community [136].

Analytical and numerical methods have aimed at predicting the residual stresses present on the machined surface and in the subsurface layer. Compared to equivalent FEM models, the analytical models present major advantages by significantly reducing computational time from days to seconds, and with a more accurate and rapidly applicable form for machining process optimisation in industry. Indeed, this simulation tool not only allows the prediction of residual stresses quickly and precisely, which is a crucial factor from an engineering perspective, but also, from a scientific perspective, it provides a deeper understanding of the effects of each parameter in detail, and thus enables the optimisation of the entire process as well.

For these reasons, during the last few decades several researchers have developed analytical methods for predicting residual stresses in machined components. Barash and Schoech [21] in their early work predicted the residual stresses present in the subsurface layer of a workpiece by using a simple slip-line model. Wu and Matsumoto [318] used an analytical model to study the effect of workpiece hardness on the pattern of residual stresses induced on the workpiece surface. Liang and Su [175] developed a predictive model for residual stresses in orthogonal cutting. Also, Liang et al. [176] subsequently developed a physics-based model to quantitatively evaluate the effects of cutting conditions and tool geometry parameters according to pre-specified surface residual stresses resulting from machining. Ulutan et al. [293] developed a comprehensive analytical model for residual stresses in machining. Lazoglu et al. [169] subsequently presented an enhanced analytical model by using the superposition of thermal and mechanical stresses on the workpiece, followed by a relaxation procedure. Accurate residual stress profiles, both in feed and crossfeed directions, were achieved in less than a minute (Fig. 24). However, correlations between the machining-induced residual stresses and the fatigue life of machined components are not well established in these predictive models due to complexity of the process variables and the geometric attributes involved. Also, other important aspects, such as the sequential cuts and the pre-stress conditions due to prior manufacturing processes, etc., are difficult to investigate with analytical models. They can however, be taken into account by the FEM models. Therefore, although this type of numerical models is more time-consuming than the analytical model, the possibility of using more complex material behaviour models that may also include sub-models for capturing complex phenomena (such as phase transformations, dynamic recrystallisation by user-routine) enables FEM models to predict residual stress profiles comprehensively and more precisely [126].

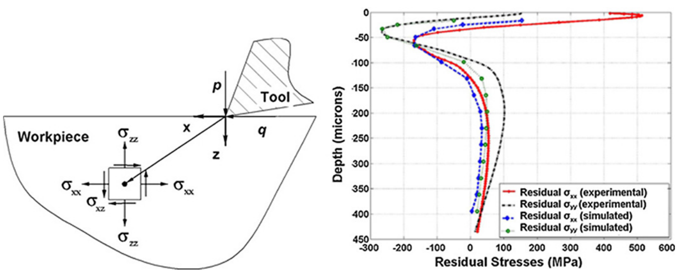


Fig. 24. (a) Coordinate frame used in the analytical model and (b) comparison of simulated and experimentally measured residual stresses (v_c = 25 m/min, f = 0.15 mm/rev; tool tip radius, r_n = 51.6 μm, Tool rake angle = 0°) [169].

Sasahara et al. [252] used FEM to predict the effect of the cutting sequence and cutting parameters on residual stresses and strains. The residual stresses can be controlled by the setting of the cutting conditions in the sequential cutting process [70,219,220]. Similar results have been obtained by Guo and Liu [108]. More specifically, Outeiro et al. [220] found that the surface circumferential residual stresses were more tractive as the number of cuts increased. Chen et al. [44] investigated the effect of tool-wear on residual stresses during machining of *Ti6Al4V* alloy. A finite element analysis was performed with and without a customized crack propagation module in order to separate the effects of flank wear length and the chip segmentation on the residual stress profile beneath the machined surface. Correct chip formation and temperature distributions were found to be critical for accurate prediction of the residual stress state (Fig. 25).

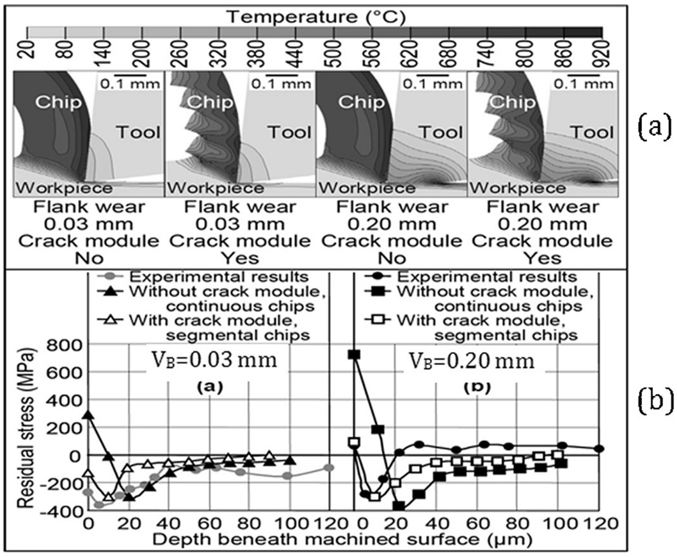


Fig. 25. (a) Chip formation and flank wear length on temperature distribution; (b) effects of chip formation and flank wear length on residual stress profile [44].

Sasahara et al. [253] developed a thermo-elastic–plastic finite element model to investigate the effect of cutting parameters, tool rake angle and tool nose radius on residual stresses. They were later able to also incorporate the effect of tool-edge configurations [254] and link this to fatigue life. Additional studies of tool edge preparation have followed [126,213]. The thermal softening phenomenon has been studied by Nasr et al. [214] and later by Ulutan et al. [294]. Özel and Ulutan [229] predicted the residual stresses using a fully 3D FE model for machining *Ti6Al4V* titanium and *IN100* nickel-based alloys. Miguélez et al. [199] showed that pure mechanical effects can contribute to the development of tensile residual stresses. Indeed, it was demonstrated that tensile residual stresses could be generated near the machined surface even in the absence of thermal expansion. Umbrello et al. [296] showed that the correct material constitutive model is also critical in the prediction of residual stresses (Fig. 26). Other major contributions to residual stresses include: (1) dynamic recrystallisation [241,300] (2) ploughing depth [113] (3) friction [261] (Fig. 27a) (4) hardening (Fig. 27 b); (5) thermal strains and (6) initial material strength and work hardening.

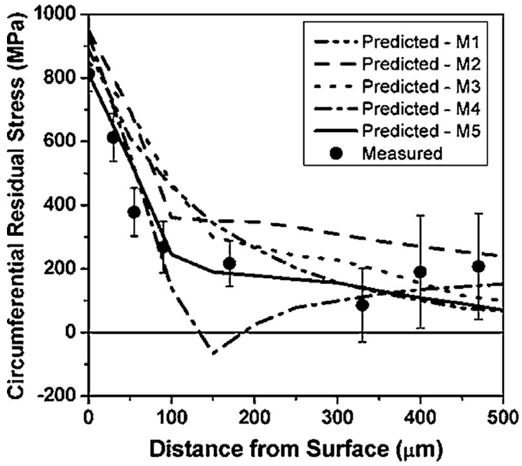


Fig. 26. Experimentally measured and numerically predicted residual stresses for different Johnson–Cook material constants during machining of *AISI 316L* steel (v_c = 200 m/min, f = 0.1 mm, a_p = 6 mm) [296].

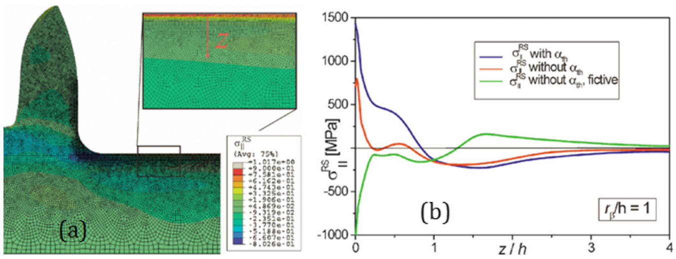


Fig. 27. (a) Residual stress distribution in cutting direction within the workpiece after cutting tool removal and temperature balance; (b)Residual stress depth profiles for *AISI 1045* steel with and without consideration of thermally induced material expansion and stress depth profile for the fictive model material [261].

In order to improve speed and accuracy, hybrid models are being developed. They are based on analytical-finite element modelling interactions, and have been aimed at accurate prediction of residual stresses in machining operations [242,298,307,324] targeting reduced the computational costs. Valiorgue et al. [308] (Fig. 28) and Mondelin et al. [206] adapted the hybrid methodology to handle the peculiarities of 3D machining and showed that at least five revolutions are necessary in order to obtain a stationary/stable residual stress profile. After empirical machining tests, mechanical and thermal loads were obtained in order to be applied in a simplified machining model.

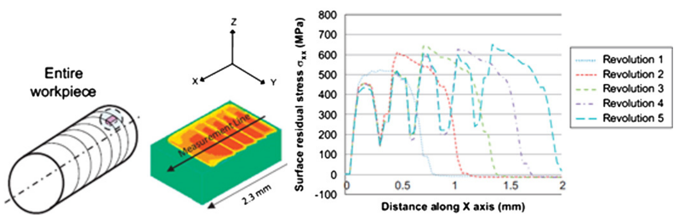


Fig. 28. Evolution of residual stresses on the surface along a measurement line with a hybrid model. (v_c = 100 m/min, f = 0.3 mm/rev, a_p = 0.3 mm. Stainless steel AISI304L) [308].

Despite the best efforts, prediction of residual stress accurately still remains difficult. Torrano et al. [288] performed a benchmark study of the most common FE codes/material models and showed the remarkable differences in the predicted residual stress profiles. Also, as stated before, in the recent CIRP-sponsored comprehensive benchmark evaluation reported by Jawahir et al. [136] it was found that prediction of residual stress profiles has still strong shortcomings, even in orthogonal cutting processes. Amongst the possible reasons, the authors attribute this to the property of the work material in the model and its subsequent effect on plastic deformation, chip formation, separation, and fracture.

5.2.2. Surface roughness

The surface quality of machined components is largely determined by their surface topography and roughness. The surface topography is strongly influenced by cutting tool geometry, cutting conditions, external loads, fixture stiffness, contact interaction between workpiece and the tool and the workpiece and tool material properties. Liao and Hu [177] developed an FEM model to investigate the influence of clamping preload and machining forces on the surface quality. Numerous other publications can be found for other factors influencing the surface quality including most notable CIRP keynote papers, including the most recent one by Jiang and Whitehouse [139].

Developing predictive models for surface quality, including machined surface topography and roughness, has attracted the attention of researchers in recent years. The development of models for the prediction of 3D surface topographies in milling operations has contributed to the understanding of the influence of cutting parameters, tool run out and cutting strategies on the roughness and on the shape and height of cusps and scallops generated in milled surfaces [5,94,167,326]. Tool cutting edge defects have also been taken into account in models for surface roughness prediction in hard turning [159] and in end milling [249]. Another factor that has a significant influence on the quality of finished surfaces is the development of vibrational instability. Surface generation models now include the effect of tool vibrations on surface topography [6,279,280]. These models allow the prediction of form errors and roughness. Models considering the workpiece vibrations have also been presented enabling the analysis of their influence on surface topography when thin-walled parts are milled [25,257].

5.2.3. Phase transformation

With rapidly increasing computational tools and capabilities for analytical modelling, analytical methods for predicting microstructure and phase transformation in machining are emerging. Chou and Evans [54] used analytical approaches to predict white layer formation by assuming that it is due to a thermally driven phase transformation. However, a comprehensive understanding of other material characteristics in the machining-affected layer, such as metallurgical changes, dynamic recrystallisation, etc., using analytical models was still lacking. Only recently researchers have begun quantitative modelling of these aspects [136]. In particular, during the last few years, a greater emphasis has been placed on developing numerical models for predicting microstructure and phase transformation. The capability of utilising more complex material constitutive models including complex material descriptions such as the effects of composition, dislocations, phases, grain size, mixture materials, etc., enables numerical models to predict various microstructural changes comprehensively and more accurately [99]. In general, numerical modelling techniques also have the ability to allow the development of specialized "user subroutines" that can be implemented for capturing complex phenomena, such as phase and microstructural transformations, dynamic recrystallisation, etc. Ramesh and Melkote [241] presented a FE model for continuous white layer formation by modelling the problem as quenching under thermally dominant cutting conditions to promote phase transformations. The effects of stresses and strains on the transformation temperature, transformation plasticity and volume expansion were considered by developing extensive subroutines for use within the FE code. Although this work represents the first attempt to simulate the white layer formation by implementing physics-based models (Fig. 29), some drawbacks are noted by the same authors: (i) a predefined separation plane is required; (ii) chip segmentation effects, which are known to occur in hardened materials, were assumed to have no influence on the surface behaviour, and thus neglected; and (iii) the orientations of

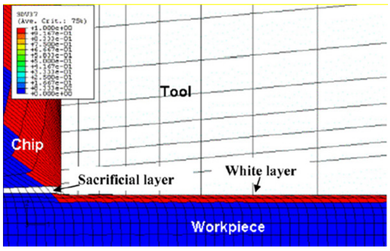


Fig. 29. Regions showing phase transformation during machining [241].

elements in the chip prior to the start of machining are altered, and the basic shape of the chip is not changed. Fischer and Bandar [85] proposed a FE model for continuous white layer formation based on the austenitisation of the surface layer and subsequent martensite formation due to rapid cooling. Material properties were modelled by a mixture rule on each element, and the hardness-based flow stress model for *AISI 52100* steel, proposed by Umbrello et al. [295], was used for defining the single mixture phases. Their results show a good agreement in both white layer thickness and hardness with those experimentally found by Han [115]. Implementation of physics-based models requires the use of large experimental databases, developed from complex metallographic analyses and time-consuming procedures for the identification of microstructure law coefficients. Most of these models have not been validated for a sufficient number of realistic cases and thus, their accuracy remains practically unknown [22]. Also, only the diffusion less type transformation (time-independent) and related microstructural changes can be realistically predicted. Due to these weaknesses FE models based on advanced empirical methods were recently proposed [299,301]. Empirical models may be very useful in practical situations since they could be easily implemented in FE codes and, through their calibration, the diffusion type transformations (e.g., dark layer formation due to tempering transformation during machining of hardened steels) can be properly simulated. Fig. 30 shows predicted white and dark layers from machining of *AISI 52100* steel. Caruso et al. [37] extended the previous work of Umbrello [301] to simulate the grain size evolution during orthogonal hard machining of *AISI 52100* steel using the *Zener-Hollomon* parameter to correlate the grain size change with strain-rate and temperature. They found that the simulated mean grain size in the white layer formed is of

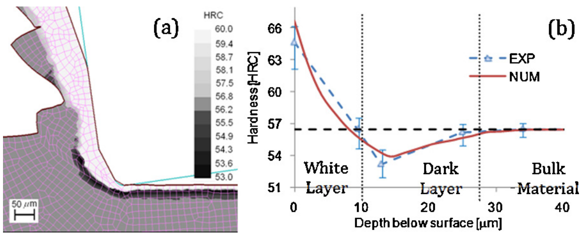


Fig. 30. (a) Simulative microstructural changes; (b) experimentally measured and numerically predicted near-surface hardness [299].

sub-micron range, and this agreed with experimental observations [240]. More recently, Pu [238] developed similar FE methodology for predicting the grain refinement induced by dynamic recrystallisation (DRX) during dry and cryogenic machining of *AZ31 Mg* alloy. *Zener-Hollomon* parameter and *Hall–Petch* relationship were implemented by user routines. Fig. 31 (a) shows the comparison of the grain size and the hardness modification between predictions and experiments from cryogenic machining. Similarly, Rotella et al. [246] developed 3D FE model for predicting the grain refinement and hardness modification during dry machining of AA7075-T651 alloy (Fig. 31b).

5.3. Microstructure

Material microstructure has a remarkable effect on the machining process performance (e.g., different tool-life results for the same material composition) and thus, fundamental understanding of the effects of material microstructure and modelling of its transformation is a key aspect for the near future.

Simoneau et al. [272] modelled the machining of an *AISI 1045* workpiece as a mixture of two different materials. The material representing pearlite was three times harder than the material representing ferrite, and for both materials a rate-dependent Johnson-Cook formulation was used. Dimples on the machined surface are predicted with the model, and the existence of these dimples was confirmed by SEM.

Recently, Abouridouane et al. [1] proposed a new 3D multi-phase FE model for micro cutting of ferritic–pearlitic carbon steels to understand cutting, ploughing, tribological and heat transfer mechanisms at the microscale. Material behaviour was modelled in a similar manner to that proposed by Simoneau et al. [272], and was based on the concept of a representative volume element (RVE), introduced by Hill [122]. Fig. 32 shows the initial 3D RVE for the two-phase carbon steel *C45* and FE model validation.

Schulze et al. [262] recently presented an FE model to predict phase transformations in cutting of *42CrMo4* steel. Particularly, they developed an FE model capable of predicting the state of austenisation, which is, together with the temperatures, essential for the morphology and the characteristics of the generated martensite. Thus, the model is capable of representing even the so called short-time austenisation, which dominates the phase transformations in cutting processes. The transformation due to thermally induced diffusion and the resulting phase fractions are calculated (Fig. 33). Therefore, the model can simulate the generation of ferrite/perlite, bainite and martensite.

5.4. Burr formation

With the increasing demands for part quality, functional performance and cost reduction, there is pressure to minimize or eliminate deburring. During the last decade, predicting and understanding burr formation has received much attention [67,86,105,117,200,215,234,287]. Although some analytical models have been proposed [14,287], FE models are now generally used for simulation of burr formation for operations ranging from drilling [105], milling [120] to turning [14,171].

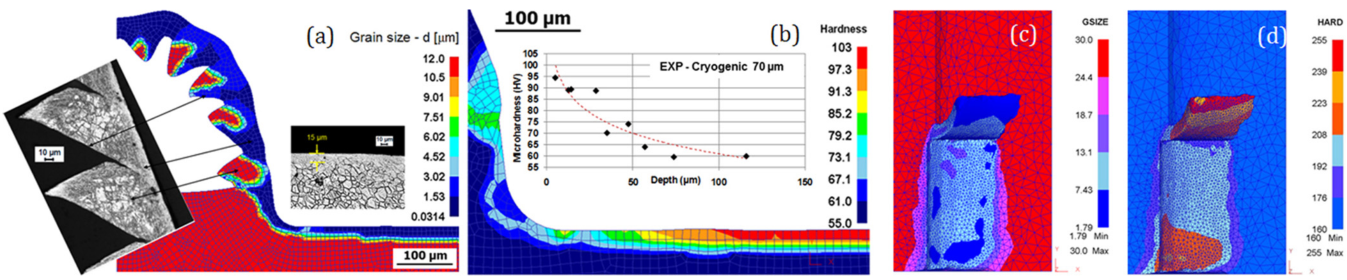


Fig. 31. (a) Predicted and experimental grain size distribution and (b) hardness modification on the machined surface and subsurface in cryogenic machining (v_c = 100 m/min, f = 0.1 mm/rev; tool edge radius, r_n = 70 μm, rake angle = −7°) [238]; (c) Predicted grain size (μm) and (d) Vickers hardness (v_c = 320 m/min, f = 0.1 mm/rev; a_p = 0.5 mm; tool nose radius, R = 0.8 mm)[246].

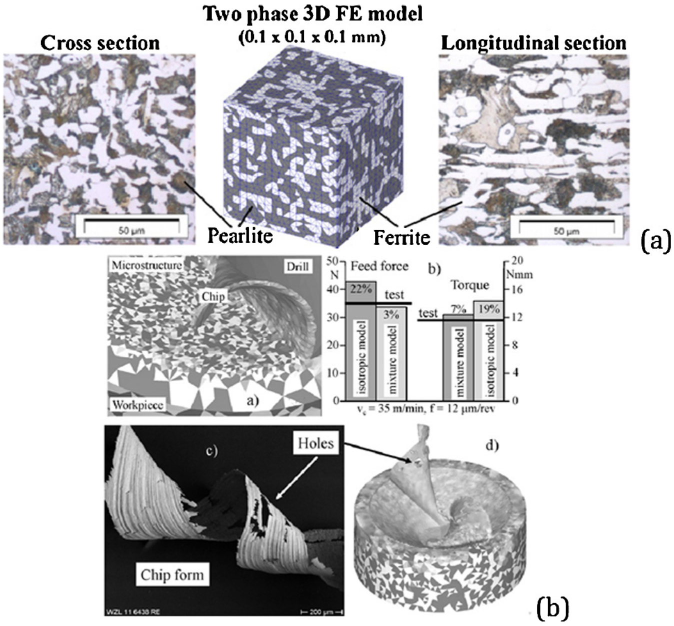


Fig. 32. (a) Generated 3D RVE for the two-phase carbon steel *C45*; (b) FE model validation by comparing chip-form, feed force and torque [1].

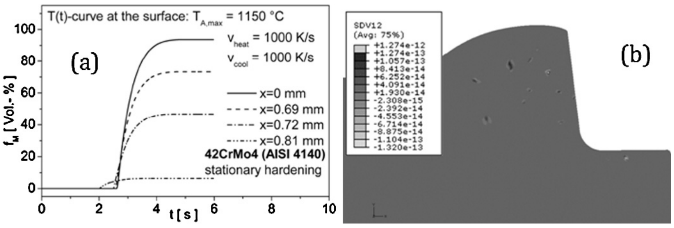


Fig. 33. (a) Characteristics of the fractions of martensite $f_M(t)$ in relation to time and different distances from the surface; (b) 2D-FE simulation of the orthogonal cutting of *42CrMo4* steel and the resulting transformation temperatures due to phase changes [262].

Hashimura et al. [117,119] developed a basic model of burr formation for orthogonal and oblique cutting. The FE-simulation agreed well with experiments including the five stages of burr formation [120]. Similar FE modelling of burr formation in orthogonal cutting was performed by Park and Dornfeld [233] and Leopold et al. [172] (Fig. 34). Stoll et al. [278], Hashimura and Dornfeld [118], Guo and Dornfeld [105] and Choi et al. [53] have developed models for burr formation in drilling, ultrasonically assisted machining, and milling.

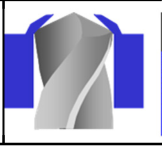
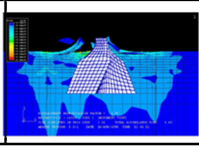
Burr formation mechanism	Proposed burr formation mechanism [Kim01a]	FEM simulation	High-speed camera image [Furness, 1998]
(e) Final burr			

Fig. 34. FEM simulation of burr formation in drilling [201].

Leopold and Wohlgemuth [173] investigated the influence of the highly negative hydro-static pressure region (hydrostatic bowl) right below the cutting edge (Fig. 35 a), and showed that a larger hydrostatic bowl improved burr formation. Toropov and Ko [287] using an analytical model for burr formation in the feed direction during turning, predicted the height and thickness of the burr, and described the burr development process from its

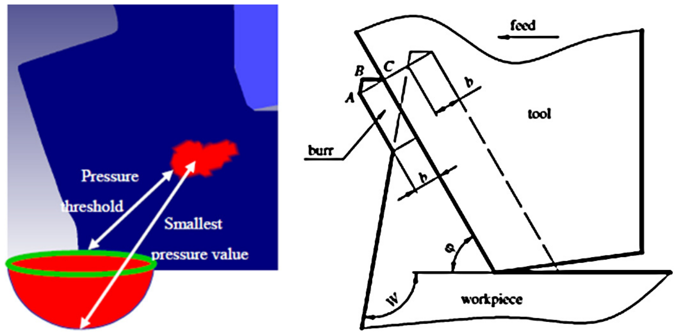


Fig. 35. (a) Hydrostatic Bowl in FEM simulation [173]. (b) Burr development in turning [287].

initiation until the burr is fully formed (Fig. 35b). This model considers two cases: continuous burr development, when the burr has grown uninterruptedly; and discontinuous burr generation, when the burr that is formed is cut-off and is renewed with each revolution of the workpiece. Comparison with experiments showed that the model could predict burr height approximately within 30%. A 3D finite element model was developed by Guo and Dornfeld [104,105] to investigate the mechanisms of drilling burr formation at the hole exit, showing correlations between the thrust force and burr size. Min et al. [200–202] developed a nonlinear elastic–plastic 3D finite element model for burr formation in drilling *AISI 304L* stainless steel, and showed good correlation of thrust force at the burr initiation point. Kim and Dornfeld [151] proposed an analytical model to predict the final drilling burr. Choi et al. [53] modelled the burr formation when drilling multi-layered material, showing good agreement with experimental measurements. Tool geometry also has a remarkable influence on burr formation. Fischer [84] summarized the influence of rake angle, cutting edge radius and tool-wear by observing the numerical results obtained by FE analysis. High-positive rake tools were shown to produce a smaller burr and the size of the burr was less sensitive to wear than it is for the zero rake tools. This could give some guidance to the manufacturers in selecting suitable tools. Leopold and Wohlgemuth [173] identify burr formation on crossing holes and on micro parts as major focus areas of future study.

5.5. Tool-life/tool-wear predictions

Tool-wear affects workpiece quality significantly. Consequently, tool-wear prediction and tool substitution policy are very important for minimising the machining costs and optimising performance. Usui et al. [306] presented an analytical equation to predict both crater and flank wear of tungsten carbide tools for a range of tool shapes and cutting conditions in practical turning operations based on "calibration" with orthogonal cutting.

In the early 1990s Teitenberg et al. [283] proposed an analytical tool-wear model to predict the flank wear during milling. Jawahir and his co-workers [131,134] presented a semi-analytical method to predict progressive tool-wear and tool failure in machining with complex coated grooved tools under dry machining conditions. Choudhury and Srinivas [55] presented a mathematical model to predict flank wear precisely in a turning process and compared it with experimental results. Kannan et al. [144] presented an analytical model for the prediction of tool flank wear progression during orthogonal machining of aluminium based metal matrix composites (MMCs) based on the energy consumed during machining. More recently, Marksberry and Jawahir [189] developed a new tool-life/tool-wear predictive model for machining with complex grooved tools under near-dry machining conditions (NDM), with a focus on Minimum Quantity Lubrication (MQL). More accurate estimates of tool-wear are made by using the new predictive model for NDM compared the results with dry machining models. Yen et al. [321] implemented the tool-wear rate models in a commercial FE code for predicting the evolution of tool-wear.

Usui's wear model [153,154,304] was used to calculate the wear rate of the uncoated carbide tool in machining of carbon steel. The location of the maximum wear rate and the low wear rate close to tool radius were consistent with the experimental results. Although, the results of this first implementation of the tool-wear rate models were good, several problems were identified by the same authors: (i) the simulation study was made with worn tool initially including a pre-defined wear land of 0.06 mm on the flank face; when a sharp tool is used, the predicted wear rate on the flank face is one order of magnitude smaller than that on the rake face. This problem was improved by using a new tool-wear model especially developed for the simulated cutting condition [90]; (ii) the tool geometry was manually updated; (iii) the selection of a suitable cutting time increment was very difficult to perform without doing experiments in advance. Xie et al. [320] implemented a Python-based tool-wear estimate programme, which calculated tool-wear based on the wear rate model, and then updated the tool geometry and the tool-wear progress in a turning operation (Fig. 36). This first attempt on implementing an updating procedure by external calculation did not provide good comparison between the numerically predicted and the experimental tool-wear parameters. According to the authors [320], the discrepancy may be caused by (i) incorrect tool-wear calibration, (ii) an overly simplified friction model and (iii) the work material model.

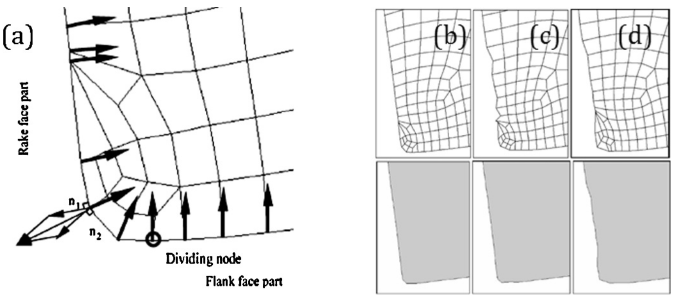


Fig. 36. (a) Wear direction vectors (thick arrows) of tool face nodes. Changes of the mesh during tool updating steps (b) at the beginning ($t = 0$ s) and during the process: (c) $t = 5$ s and (d) $t = 46$ s [320].

The recent progress in powerful remeshing algorithms, tool-wear model calibration and tool-wear updating procedures have made possible tool-wear modelling during orthogonal machining. Klocke and Frank [156] used Usui's wear model and an automatic updating procedure to predict tool-wear on a CBN tool in hard turning. Similar work has been carried out by Rizzuti et al. [244], who investigated the flank wear in honed and chamfered CBN tools during hard machining of *AISI H13* with different initial workpiece hardness. More recently, Schulze and Zanger [263] predicted the flank wear in orthogonal machining of *Ti6Al4 V* with a tungsten carbide (*WC*) tool. Lorentzon and Järvstråt [181] developed a FE tool-wear modelling method that can predict the worn geometry quantitatively in machining of nickel-based alloys with a cemented carbide tool. They concluded that the friction model has a major influence on the simulated wear profile. The proposed friction analysis, with a lower coefficient in the area around the tool tip, which was consistent with Usui's empirical wear equation, shows excellent agreement with experiments. Takeyama and Murata [281] simulated the evolution of tool-wear by implementing a diffusion wear rate equation in a FE software. This equation was first used by Filice et al. [82] for steel, who calculated the wear rate from fundamental variables values (temperature, stresses...). The method consisted in updating the geometry by moving the nodes of the tool according to the calculated wear rate.

Also, Filice et al. [82] used an inverse approach and demonstrated that parameter D in the equation by Takeyama and Murata [281] (Eq. (26)) is not a constant, but is strongly dependent on the temperature. Due to this modification, reasonably good accuracy was achieved for both the crater and the flank wear, and the proposed methodologies (tool-wear calibration, thermal aspect consideration, and tool updating procedure) can be regarded as significant developments.

Nowadays, the substantially enhanced hardware and software capabilities and efficiencies make 3D models highly effective in simulating a range of machining operations. In particular, remeshing algorithms permit managing complex geometries with required accuracies despite long computational times. Attanasio et al. [10] performed a scale-up simulation of the knowledge acquired with 2D models to 3D in order to obtain results, which are closer to the industrial needs. Flank and crater wear evolution was predicted utilising a diffusion wear model implemented into an Arbitrarian Lagrangian Eulerian (ALE) numerical formulation when *AISI 1045* steel was machined using an uncoated WC tool. A 3D finite element analysis, provided with a new 3D updating procedure for the dynamic prediction of the tool-wear, was carried out (Fig. 37). A good agreement was achieved between the numerical and experimental maximum flank wear, V_B, and the crater depth and position (K_T and K_M). In turn, larger errors were found for the crater area prediction: the simulated worn area was always lower than the experimental one (Fig. 38 a). They attributed this discrepancy to the fact that the implemented wear model considered only the diffusion wear mechanism, which is known to take place on the tool rake face at temperatures higher than 800 °C. Therefore, the tool areas at temperatures lower than this threshold are not considered worn in the simulation. Subsequently, Attanasio and Umbrello [11] improved their proposed wear model also by considering the abrasive wear. This gave good results for crater wear simulation (Fig. 38b).

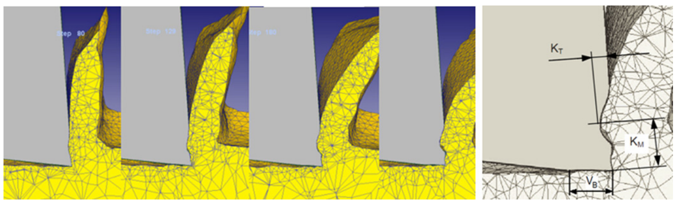


Fig. 37. Tool-wear progression in 1, 2, 4 and 6 min ($v_c = 160$ m/min, $f = 0.25$ mm/rev, $a_p = 1.5$ mm) in machining of *AISI 1045* steel [10].

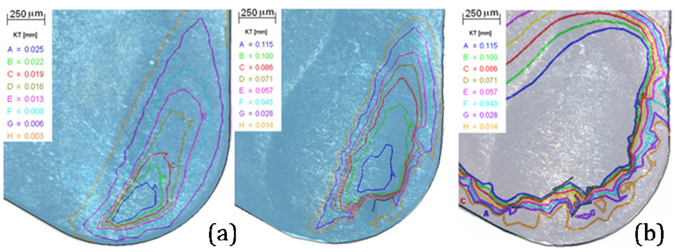


Fig. 38. Experimental and simulated crater wear during machining of *AISI 1045* steel at $v_c = 160$ m/min, $f = 0.25$ mm/rev and $a_p = 1.5$ mm: (a) after 1 and 6 min with only diffusion wear implemented [10]; (b) after 6 min with the adopted abrasive-diffusive wear model [11].

Tool-wear studies were also performed by Klocke et al. [157] in turning of case hardened steel *16MnCr5* with cBN tool inserts. They found a clear groove formation on the flank face of the engaged cutting tool inserts, demonstrating the abrasive wear mechanisms. By implementing a similar FE strategy and methodology, Özel et al. [227] and Attanasio et al. [12] recently simulated the crater wear in term of both K_T and K_M parameters and the crater extension. Özel et al. [227] investigated the effect of multi-layered coated tool inserts in machining of *Ti6Al4V* titanium alloy (Fig. 39a), while Attanasio et al. [12] observed the evolution of the crater area when uncoated tools were used for machining mild steels (Fig. 39b). Other examples of FEM can be found in the study of coatings and

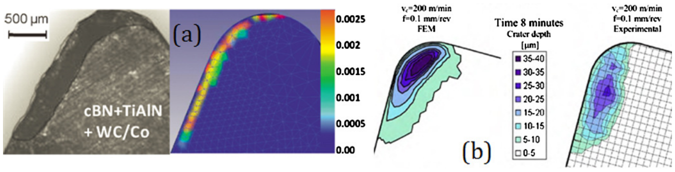


Fig. 39. (a) Experimental and predicted wear rate distributions in mm/s during machining of *Ti6Al4V* titanium alloy with multi-layered coated tool insert [227]; (b) crater wear evolution in 3D machining [12].

their influence on cutting results, particularly in the analysis of stress distributions [27,28].

Besides abrasion, adhesion could also be found in the form of adhered material in the contact area of chip and flank face. The tool-wear process in practical machining operations such as turning, milling and drilling is quite complex, because the tool-wear process is concurrent with multiple wear mechanisms on complex tool geometries and multi-layer tool coatings, and modelling methods need to be developed and implemented despite difficulties and prolonged computational time requirements. Klocke et al. [158] suggest that due to the very long simulation time, implementation of tool-wear modelling may not be appropriate in this case. Therefore, analytical models or hybrid solutions might be sought to reduce the computational time.

5.6. Machining stability

The study of machining stability naturally crosses the boundary between process modelling and modelling of the machine tool. The primary cause of instability in machining processes including turning, boring, drilling, milling, and broaching is regenerative chatter. The simplest case of one-dimensional regenerative chatter is given in Eq. (28). While the cause of the chatter is regeneration of chip thickness, the stability solution differs significantly between interrupted cutting operations such as found in milling and indexed boring, drilling and with tools having non-uniform pitch and helix angles. Instead of having single delayed differential equation (e.g., Eq. (28)), the system can be described by multiple, coupled delayed differential equations with multiple time delays and time-dependent coefficients.

Regenerative instability occurs due to a coupling between the dynamic deflections of the machine tool and the effect on the cutting process itself. Delayed feedback occurs whereby the current uncut chip thickness results from the current position of the tool $y(t)$ (outer waviness) and the tool position on the previous rotation of the spindle $y(t-T)$ (outer waviness). This produces a delayed-differential equation (Eq. (28)),

$$m\ddot{y} + c_y\dot{y} + k_y y = K_r a(h_0 - (y(t) - y(t-T))) \tag{28}$$

where m, c_y and k_y are the lumped mass, damping and stiffness of the relevant mode of the entire machine tool structural loop. The parameter K_r is the radial cutting force coefficient, a is the width of cut, and h_0 is the nominal chip thickness. Analysis of this equation (or similar equations for other machining operations) produces the well-known stability lobe diagram with alternating regions of stability corresponding to the cases where the inner and outer waves are in-phase, and thus, the chip thickness is constant. This occurs when the spindle period T is an integer multiple of the period the machine-tool loop vibration $(1/f_n)$.

The stability of the machining system has long been studied in detail beginning with the pioneering and concurrent research of Tlusty and Polacek [284] and Tobias and Fishwick [286]. This research work has been examined in detail by two previous CIRP keynote papers by Tlusty [285] and Altintas and Weck [3], and it is beyond the scope of this paper to make a thorough review of this specific topic. In fact, as pointed out by Altintas and Weck [3], much of the research on machine tool chatter has resulted in several commercial products that are used in industry. This translation to industry was accelerated by the extremely rapid development of high-speed machining technology in the 1980s & 1990s. There are several commercial products that allow users to choose stable parameters in high speed machining and correct choice of parameters, while counterintuitively leading to increases in productivity that may approach an order of magnitude [274] and enable the manufacture of components that traditionally would have been thought impossible [60].

A critical aspect of this keynote paper is to highlight those areas where process modelling can couple with modelling of the machine tool to produce more accurate results for chatter prediction: (1) understanding process damping; (2) identifying the mechanistic cutting force coefficients (e.g., K_i in Eq. (27)) that are at the heart of chatter prediction routines; and (3) designing tools that minimize the physical causes of chatter.

In more complex machining operations such as milling, drilling, etc., process models could help identify the cutting force coefficients in the presence of complex cutting geometries. Current chatter prediction methods still rely on experimental measurements of these force coefficients for optimum accuracy. Measuring cutting force coefficients is time-consuming and expensive, and also they change with material properties and tool type, tool coating, etc. A cutting force model, that could accurately predict the cutting force coefficients for complex tool geometry, type, coating etc., would benefit research in the prediction of machining stability greatly.

It has been suggested that process models can eventually be coupled with models of the machine tool producing a truly *virtual machine tool* where predictions of stability can be made before the machines and processes are even developed [4,29].

5.7. Part distortion

One of the major prediction requirements in machining process modelling is part distortions that occur largely due to bulk and surface residual stresses. The main goal here is to predict distortions occurring during multi-operation machining of industrial components. Typically, the workpiece undergoes some distortions during the machining process, which induces a deviation between the theoretical cutting and the real cutting paths, and thus a shape error occurs due to the fact that the real cut thickness is not the theoretical one. In order to model this phenomenon, some approaches can be adopted [160] to allow boundary conditions play an essential role. The elements in the cutting path must be deleted, and then machining forces and the heat fluxes must be introduced in the new surface.

Masset et al. [193,194] developed an efficient finite element model based on the well-known super-element reduction algorithm. They assume that tool and machine-tool are perfectly rigid, and they neglect thermal deformations. Under the linear and quasi-static hypotheses, the equations representing the system behaviour are condensed in order to keep a limited set of degrees of freedom representing the machined surface and the clamping zones. The resulting reduced system is then inverted one time for a given configuration of the clamping. Due to these two steps, the response of the system may be obtained in a few seconds for any tool geometry, tool trajectory or cutting conditions.

Urresti et al. [302] presented a distortion analysis carried out on a typical aerospace turbine disc where the heat treatment process was modelled in order to obtain a representative residual stress pattern prior to the machining modelling. They assume axi-symmetrical response and during the modelling of machining, predefined element domains were removed. The rough machining predictions were validated against experimental displacement measurements obtaining good results. Then, the machining model was used to predict the in-process disc behaviour during the roughing and finishing operations, and different cutting strategies were proposed and analysed for part distortion (Fig. 40). In order to decouple the workpiece mesh and cutting path, Pierard et al. [236,237] presented a new approach based on the level-set method. Marusich et al. [192] developed a finite element model to

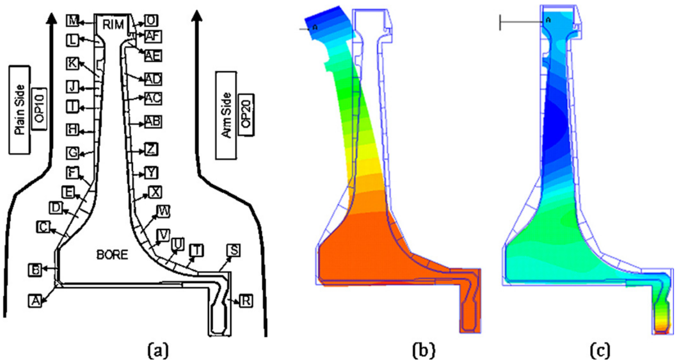


Fig. 40. (a) Rough machining sequence employed for the model validation. Domains are represented in order of operation by the letters. (b) Disc distortion after (c) OP10 plain side machining, and (b) after OP20 drive arm side machining [302].

predict and control distortions in the machining of airframes for aircraft.

This model considers the machining-induced residual stresses, as well as the bulk stresses, in the material from the manufacturer. It is the combination of these two residual stress components, coupled with the geometry of the part and type of material, that allows accurate prediction and control of the shape and magnitude of deformation in large thin walled structures. It can be used to determine the ideal cutting conditions for the process, as well as determine the best location within the stock plate to machine the part from.

6. Process modelling needs for efficiency, effectiveness and applicability in industry

From the above review and analysis of the current state-of-the-art on modelling of metal machining operations, it is clear that industry application of machining models is very limited largely due to the following reasons:

- Industry applications of machining models are need-based, cost-concerned and require simple user functions with no or little prior knowledge or training for use.
- Industry interests are limited to predictive performance models, and not predicting fundamental variables.
- Direct application of currently available predictive performance models for specific operations on the shop floor is limited, as most models developed by researchers are only laboratory-validated, and not shop floor-tested.

Table 2
Current state-of-the-art in modeling of metal machining processes.

Status	Fundamental Variables			Industry-relevant Outcomes					
	Forces	Temperatures	Stresses, Strains, Strain-rates,	Chip geometry/ Chip breakability/ Chip control	Surface integrity (Residual stresses, Microstructure, Phase change, Hardness,Surface roughness, etc.)	Burr formation	Tool-wear/ Tool-life	Stability	Part distortions
Achieved	• Prediction of forces in 2D and 3D with mechanistic, analytical and numerical models	• Prediction of temperatures in 2D and 3D with analytical and numerical models	• Prediction of stresses, strains, strain-rates in analytical models, and in 2D and 3D FEM models	• Analytical, empirical and FEM models for chip formation • Segmented chip formation in 2D and 3D FEM models • Chip breakability in analytical, empirical and 2D and 3D FEM models • Partial development of commercial software for producing desired chip geometry and breakability	• Empirical prediction of surface roughness • Residual stress prediction in 2D analytical, and 2D and 3D FEM models • FEM models for predicting hardness, microstructure, phase change, multi revolution in single and sequential cut/ multi-pass operations • Commercial software capability for predicting partial surface integrity	• 2D and 3D FEM burr formation models • Empirical models for burr control	• Empirical models for tool-life predictions • Implementation of tool-wear models in commercial FE codes • Tool-wear models (flank and crater) in orthogonal machining • First steps in 3D tool-wear (flank and crater) modelling	• Predictive capability for stability • Commercial software development for choosing the stable cutting conditions	• 2D FEM models for predicting prior operation effects (forging, quenching, etc.) • 2D machining modelsfor correlating bulk and surface residual stresses with part distortion. • Commercial software capability for predicting part distortion
Ongoing and Future Work	• Reduction of uncertainty • Applications on new work materials, advanced tool geometries and coatings	• Availability ofexperimental equipment for 3D Measurement and analysis • Enhancement of analytical 3D models • Accurate and fast models for various temperature profiles	• Developing experimental techniques for 2D and 3D measurements • Enhancement of 3D models for industry relevant machining operations	• Accurate and Easy-to-use models. • Knowledge-base development for information modelling and universal chip control predictions	• Study of the effects of complex 3D tool geometry, tool-wear, tool and workmaterialson surface integrity • Macro/micro/ nano-scale models for predicting surface integrity/ residual stresses • Predictive models for micro/ nanostructure effects • Hybrid predictive models	• Physics-based analytical models for burr control	• Model to predict tool-wear in practical machining operations with complex grooved tools for multiple wear mechanism and multi layer tool coatings • New analytical and/or hybrid predictive models	• Accurate model for predicting machining stability for complex tool geometry, coatings and a range of practical work materials	• Hybrid predictive model for complex part geometry and advanced materials
Key Publications	[4,32,77,80, 87–88,93,95, 124,138,146, 196,205,282, 309,312]	[8,10,62,72,77, 101,125,128, 146,149, 161–163,168, 211,228,309]	[24,26,40,46, 77,227,251, 261,265, 276,309]	[18,20,34, 73–74,79,93, 130,132,135, 270,290–292, 297,309]	[1,5–6,37,44,70,113, 136,169,175,206, 212–214,220–221, 229,241–242,246, 262,271–272,288, 299–301,308]	[14,53,105, 117,171–173, 200–202, 233–234,278, 309]	[10,12,35,71,131, 134,158,189,204, 227,263,321–322]	[3,23,58,279, 285]	[192–194, 236,302]

In view of the above observations, it is considered important to develop strong interactions with industry-based application personnel, and develop long-term collaborations with them to enable proper use of the new knowledge and tools developed by the researchers at appropriate levels. Mechanisms do exist in many countries for developing such strong interactions with industry through federally funded and cost-shared projects.

7. Conclusions and future outlook

This paper presents a summary of the state-of-the-art developments in modelling of machining processes during the last 15 years, since the last CIRP keynote paper on modelling of machining was produced in 1998. As seen, significant advances have been made in developing more advanced computational tools and analytical methods for fundamental modelling at the process level using the traditional 2D and 3D methods. Numerous modelling methods and techniques have emerged in recent years, with only limited application potential. Major findings of this paper are blended with statements of future directions as follows:

- Significant progress has been made in modelling of metal machining processes for predicting major fundamental variables such as stresses, strains, strain-rates, temperatures, etc. However, the use of such predictive models is limited due to the reasons listed in Section 6. With recent major advances in technology, analytical and computational models for predicting these fundamental variables (Stage I predictions) are continuously being developed and updated by researchers. However, transforming these models to develop applied models for predicting machining performance measures such as tool-life, surface integrity, chip-form/chip breakability, part distortion, machining stability, etc. (Stage II predictions) for immediate use by industry poses a significant challenge. Unless the core issues involved in such difficulty are fully understood, and are adequately addressed, the usability of fundamental models by industry will largely remain limited. Operation-level predictive models need to be developed for direct applications.
- There is an urgent need to move from 2D to 3D model development. Most fundamental variables and machining performance measures are predicted in 2D, while industry needs are in 3D models for specific operations.
- Collaborative research efforts among researchers from academia and industry-based application specialists are essential for moving forward in this regard. Benchmarking and round robin testing activities must be promoted for understanding the current level of technological advances in terms of usability and/or limitations of existing models.
- A significant difficulty in numerical modelling of metal machining is the current lack of consistent and reliable material and contact models. Databases for material properties (i.e., flow stress data) must be established for currently used and highly engineered new work materials.
- Hybrid modelling (analytical/numerical; and empirical/numerical) is needed to cut down the computational time.
- Innovative cutting tools, including advanced coatings, tool grades and complex geometry must be considered by researchers in developing future models for machining.
- Robust predictive models are needed for accommodating complex interaction among the work material, cutting tool and machine tool. This need must be viewed from a machining system perspective.
- Current knowledge on the effects of the use of cutting fluids (coolants/lubricants) is very limited. With rapidly diminishing cutting fluids, and growing applications of MQL and cryogenic methods in machining, in an effort to introduce environmentally benign, sustainable machining technologies that are equally good for producing functionally superior machined products, new machining models must be developed for such innovative applications. Table 2 summarizes the current state-of-the-art in modeling of metal machining processes in two broad categories: fundamental variables and industry-relevant outcomes. This table presents a list of current achievements, and a list of more challenging ongoing and future work, along with numerous key references for each major modeling variable discussed in this paper for the two-stage modeling approach adopted as shown in Fig. 1.

Acknowledgements

The authors sincerely thank to the following colleagues for their valuable contributions in preparation of this manuscript: M. Abouridouane, S. Afazov, T. Altan, Y. Altintas, P. Aristimuño, M. Arizmendi, A. Attanasio, W. Belluco, D. Biermann, K.D. Bouzakis, E. Budak, M. Calamaz, E. Ceretti, S. Chandrasekar, T.H.C. Childs, D. Dornfeld, L. Filice, C. Fischer, Y. Guo, W. Grezsik, V. Kalhori, F. Klocke, A. Kortabarria, H. Kishawy, I. Lazoglu, J. Leopold, T. D. Marusich, L. Masset, T. Matsumura, M.H. Miguelez, F. Micari, A. Moufki, Y. Mizugaki, R. M'Saoubi, O. Pierard, J.C Outeiro, J. Rech, H. Sasahara, A. Shih, J. Sölter, V. Schulze, E. Uhlmann, F. Zanger.

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