J Mater Sci (2007) 42:2002–2015
DOI 10.1007/s10853-006-0171-z

REVIEW

A review of machining theory and tool wear with a view to developing micro and nano machining processes

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Received: 12 June 2005 / Accepted: 4 April 2006 / Published online: 23 January 2007
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Abstract This paper reviews the current state-of-the-art surrounding macro scale machining; it discusses how these factors will influence the future development of micro and nano scale machining. The paper begins by reviewing machining theory, and then discusses high speed machining, tool wear, tool coatings, micromachining, meso machine tool design and future applications and research directions. Tool wear is a factor that determines the economy of the machining process. Therefore, an extensive part of the paper is devoted to the development of materials used to coat tools; in turn, it is anticipated these coatings will used in future micro machining applications. Consideration is also given to machine structures that are required to use these cutting tools at speeds in excess of one million revolutions per minute. This review provides a timely explanation of the literature surrounding the factors required for successful micro and nano machining.

Introduction

Mechanical micro machining is a technique that has the potential to become a successful small scale manufacturing process. If the attributes of macro machining can be reduced in size to the micro scale, then a versatile manufacturing technique will be created that is capable of processing a wide variety of materials. However, simply scaling down machines is not the solution, reducing the scale from macro-to-micro presents unique problems that must be overcome. These problems include, eliminating tool wear, creating a stable machine tool structure, and overcoming the size effect (where micro tools encounter less defects, increasing the strength of the material). Therefore, the aspects of macro scale machining are reviewed, with a view to applying them at the micro and nano scales.

Machining theory

In the 1940's Ernst and Merchant [1], and Merchant [2, 3], developed models for orthogonal cutting, the case where the cutting edge is perpendicular to the direction of motion. The model described shearing of undeformed material as it passed through a primary shear zone. Earlier work by Piispanen [4], stated that the shearing process of metal is similar to cutting a deck of stacked cards; the cards are inclined at an angle φ, which matches the shear plane angle. As the cards approach the tool, they are forced to slide over each other due to the resistive force provided by the tool. Merchant [2] began the metal cutting analysis by making certain assumptions, which are presented by Shaw [5]. (1) The tool is perfectly sharp and there is no contact along the clearance face. (2) The shear surface is a plane extending upward from the cutting edge. (3) The cutting edge is a straight line extending perpendicular to the direction of motion and generates a plane surface as the work moves passed it. (4) The chip

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does not flow to either side. (5) The depth of cut is constant. (6) The width of the tool is greater than the width of the workpiece. (7) The work moves relative to the tool with uniform velocity. (8) A continuous chip is produced with no built-up edge. (9) The shear and normal stresses along the shear plane and tool are uniform.

The forces acting between the chip and tool are isolated in a free body diagram; these forces act perpendicular to (N_C) and along (F_C) the tool face, and horizontal to (F_P) and vertical (F_Q) to the direction of motion, and finally, along (F_S) and perpendicular (N_S) to the shear plane. These forces can be rearranged and applied at the tool tip, whilst also being contained within a circle. This is Merchant's [2, 3] circle of cutting forces, and is fundamental to metal cutting theory. From this circle, a range of equations can be generated that describe the cutting process. These equations are listed below, where α is the angle between the vertical and the tool face called the rake angle, and φ is the shear plane angle.

$$F_S = F_P \mathrm{Cos}\,\phi - F_Q \mathrm{Sin}\,\phi \tag{1}$$

$$N_S = F_Q \mathrm{Cos}\,\phi + F_P \mathrm{Sin}\,\phi \tag{2}$$

$$N_S = F_S \mathrm{Tan}(\phi + \beta - \alpha) \tag{3}$$

where β is the friction angle and μ the coefficient of friction and is given by;

$$\mu = \mathrm{Tan}\,\beta \tag{4}$$

$$F_C = F_P \mathrm{Sin}\,\alpha + \mathrm{F}_Q \mathrm{Cos}\,\alpha \tag{5}$$

$$N_C = F_P \mathrm{Cos}\,\alpha - F_Q \mathrm{Sin}\,\alpha \tag{6}$$

$$\mu = \frac{F_C}{N_C} \tag{7}$$

$$\mu = \frac{F_P \mathrm{Sin}\,\alpha + F_Q \mathrm{Cos}\,\alpha}{F_P \mathrm{Cos}\,\alpha - F_Q \mathrm{Sin}\,\alpha} \tag{8}$$

$$\mu = \frac{F_Q + F_P \mathrm{Tan}\,\alpha}{F_P - F_Q \mathrm{Tan}\,\alpha} \tag{9}$$

The shear stress τ is given by;

$$\tau = \frac{F_S}{A_S} \tag{10}$$

Where,

$$A_S = \frac{bt}{\mathrm{Sin}\,\phi} \tag{11}$$

Here, b is the width of cut and t is the depth of cut, therefore the shear stress is;

$$\tau = \frac{(F_P \mathrm{Cos}\,\phi - F_Q \mathrm{Sin}\,\phi)\mathrm{Sin}\,\phi}{bt} \tag{12}$$

Similarly the normal stress σ is given by

$$\sigma = \frac{N_S}{A_S} \tag{13}$$

$$\sigma = \frac{(F_P \mathrm{Sin}\,\phi + F_Q \mathrm{Cos}\,\phi)\mathrm{Sin}\,\phi}{bt} \tag{14}$$

An equation for φ is still required. It is found experimentally that when certain metals are cut there is no change in density. The subscript C refers to the chip and l is the length of cut, therefore, it follows that

$$tbl = t_C b_C l_C \tag{15}$$

It is found experimentally that if $b/t \geq 5$ the width of the chip is the same as the workpiece thus

$$\frac{t}{t_C} = \frac{l_C}{l} = r \tag{16}$$

Where r is the cutting ratio, or chip thickness ratio;

$$r = \frac{t}{t_C} = \frac{AB\mathrm{Sin}\,\phi}{AB\mathrm{Cos}(\phi - \alpha)} \tag{17}$$

Where AB refers to the length of the shear plane. Solving for the shear angle φ.

$$\mathrm{Tan}\,\phi = \frac{r\mathrm{Cos}\,\alpha}{1 - r\mathrm{Sin}\,\alpha} \tag{18}$$

The work length may be determined by weighing the chip, if the chip weighs w_C and γ' is the specific weight of the metal then

$$l = \frac{w_C}{tb\,\gamma'} \tag{19}$$

The shear strain γ is given by;

$$\gamma = \frac{\mathrm{Cos}\,\alpha}{\mathrm{Sin}\,\phi\,\mathrm{Cos}(\phi - \alpha)} \tag{20}$$

The cutting velocity, V, is the velocity of the tool relative to the workpiece and is directed parallel to F_P. The chip velocity, V_C, is the velocity of the chip relative to the tool and is directed along the tool face.

The shear velocity V_S is the velocity of the chip relative to the workpiece and direct along the shear plane.

$$V_C = \frac{\text{Sin } \phi}{\text{Cos}(\phi - \alpha)} V \quad (21)$$

Or,

$$V_C = rV \quad (22)$$

$$V_S = \frac{\text{Sin } \alpha}{\text{Cos}(\phi - \alpha)} V \quad (23)$$

Or,

$$V_S = \gamma \text{ Sin } \phi \ V \quad (24)$$

The shear strain rate γ'' is given by

$$\gamma = \frac{\text{Cos } \alpha}{\text{Cos}(\phi - \alpha)} \frac{V}{\Delta y} \quad (25)$$

Where Δy is the thickness of the shear zone and is often approximated by assuming its value is equal to the spacing between slip planes. It is not always convenient to measure all the quantities required to apply the equations. Therefore, Merchant and Zlatin [6] produced a number of nomographs that can be used to obtain some of these quantities without the need for extensive experiments.

Often, machining operations such as milling, deviate from the idealized cutting conditions. In this case machining is not orthogonal, and there are multiple cutting points, which create machining marks in the workpiece. Machining marks are a series of peaks and valleys called scallops, the height of which, h can be determined by

$$h = \frac{f}{4(D/f) \pm (8n/\pi)} \quad (26)$$

where D is the diameter of the cutter, f is the feed per tooth and n is the number of cutting teeth.

A situation can arise where a built up edge forms, this is a build up of workpiece material on the tool. The formation of a built up edge is not accounted for in the idealized assumptions and is usually detrimental to the cutting process. As cutting progresses, the build up of material gradually increases until the machining forces are large enough to shear it off, then another built up edge beings to form; the process therefore, is dynamic. The built up edge is formed by part of the chip welding to the tool. The velocity of the chip at the interface between the tool and chip is zero; at the free surface of the chip its velocity is at a maximum; therefore, there is a velocity gradient across the chip, which is facilitated by internal shear. This internal shear causes generation of additional heat, and this tool-chip interface is known as the secondary shear zone, it is often a source of tool wear.

The problem in applying the equations is determining the shear angle; depending on the assumptions made, a variety of equations can be generated for the shear angle. Stabler [7], Lee and Shafer [8] and Oxley [9] developed such equations. Assumptions matching the conditions likely to be encountered are usually used, and this highlights the complexity of modeling the cutting process; in fact no universal model has been generated that can be applied to all situations of metal cutting. This is particularly true at the micro scale where the 'size effect' must be considered. The size effect is a result of a reduction in scale, as materials decrease in size, the likelihood of encountering a defect decreases, thus the material approaches its theoretical strength. Similarly a micro or nano cutting tool is less likely to encounter a defect. An implication for micro machining is that an increase in the material's strength could mean the cutting forces are elevated. A comprehensive review of micro scale machining that takes account of the size effect, is provided by Shaw [10] and Shaw and Jackson [11].

High speed machining

High speed machining is usually defined by spindle speeds between 30,000 and 100,000 revolutions per minute (rpm). Schulz [12] discussed the advantages of high speed machining, however, it was found an unwanted effect of high speed machining is the rate of tool wear often increases; this is due to an increase in cutting temperature. Schulz [12] also found that optimizing the machining process could yield machining results better than finishing processes such as grinding. Eliminating post processing decreases lead-time and part cost, thus providing high speed machining with an economical advantage over conventional machining operations. The first application for high speed machining was to cut aerospace materials, such as titanium, and nickel-based alloys; cutting speeds between 30 m/min and 100 m/min were achieved with conventional carbide tools. However, these speeds can be significantly increased if ceramic tools are used, and increased speeds leads to improved machining performance. Usui et al. [13] constructed a cutting model based on an energy approach; later Usui and Hirota [14] extended the model to examine chip formation and cutting forces with a single point tool. Finally, Usui et al. [15] investigated thermally activated wear mechanisms for ceramic tools, they established the tool edge temperature by exposing a 25 μm diameter wire inserted into a Si_3N_4 insert at the rake face. Experimental work by Kitagawa et al. [16] showed that tool temperature increases with cutting speed and that rake face temperature is higher than the flank face temperature, they also found that Taylor's tool life equation could be used for calculating the life of ceramic tools.

Ozel and Altan [17] produced finite element models of high speed machining by using a variable coefficient of friction to take account of the dynamic cutting situation at the tool-chip interface; the model predicts cutting forces decrease with increasing cutting speed in the high-speed machining regime. Moufki et al. [18] predict that cutting temperatures reach between 500 °C and 1,000 °C for steel. Bailey [19] identified reasons for the varying coefficient of friction; these included dynamic factors such as the rake angle, feed and cutting speed. Therefore, precisely measuring or predicting the tool-chip interface temperature is a challenging problem. Montgomery [20] showed at large sliding velocities a reduction in the coefficient of friction is observed when the normal pressure or sliding velocity is similarly increased. Comparing predicted temperatures, to those directly measured during experiments validates these models. Temperatures are experimentally obtained by using thermocouples demonstrated by Groover and Kane [21], infrared sensors demonstrated by Wright and Trent [22] and chip microstructure analysis demonstrated by Fourment et al. [23]. Finite element analysis by Kim and Sin [24] describes the way in which chips form, this aids the understanding of how the chip microstructure was produced and gives further insight into the effect of temperature during cutting.

Trent and Wright [25] observed during slow speed machining that the condition of chip sliding is dominant, during high speed machining the condition of seizure is dominant and somewhere in between there is a transition between sliding and seizure. Seizure occurs when the apparent area of contact equals the actual area of contact; this is in agreement with the work on friction carried out by Doyle et al. [26]. Gekonde and Subramanian [27] predicted that craters resulting from seizure have a maximum depth correlating to the phase change temperature, which is sufficient to cause dislocation generation that leads to diffusion wear. Gekonde [27] also found that tool wear is the net result of mechanical and chemical wear. Mechanical wear remains constant and is independent of the cutting speed; chemical wear, however, increases with cutting speed. Metal cutting theory can predict cutting temperatures and therefore tool wear. However, there are usually a large number of measurements to be taken constants and constants to be found before the theory can be applied. This can be problematic, for example, Gygax [28] found that measuring cutting forces with a dynamometer is difficult in the case of milling due to the periodic impact of the individual teeth. There is also a balance to be struck between conducting time consuming experiments to accurately determine the required information and using sensors to rapidly provide this data; the sensors however, are usually less accurate. Rotberg [29] compromised by using a large number of sensors during experiments to gain the most accurate results in an acceptable time. The approach of validating models with experimental evidence is useful since the models can be used to predict cutting forces and determine what conditions are likely to be encountered prior to machining.

During high speed machining stability is critical for dampening vibrations. Schmitt [30], Weck and Staimer [31] and Ibaraki et al. [32] considered different methods of creating the working envelope. It was found that a hexapod structure is a more stable construction than that of a conventional milling machine; it also produces better results in terms of surface finish. This is because the hexapod does not suffer from vibrations caused by jerk, which result from the high accelerations and speeds required for high speed machining. Another problem faced by high speed machining is in achieving high rpm. Moller [33] summarized the special requirements for high-speed spindles, the best designs have no transmission, achieve high speeds with low vibration, are liquid cooled and had low inertia allowing high accelerations and decelerations. Another significant challenge identified by Moller [33] is the problem of selecting bearings suitable for operation at high speed. Cohen and Ronde [34] have proposed that hydrostatic bearings could operate successfully at high speed since they eliminate run out. If these issues can be resolved, Moller [33] lauded the advantages of cutting at high speed. In particular the predicted decrease in cutting force would be advantageous at the micro scale.

Tool wear

Establishing the point at which the tool is considered worn is important, since after this point, machining results are no longer acceptable. Tansel et al. [35] applied neural networks to this problem, based on past failures the critical point of future tool wear could be identified by training neural networks to monitor the cutting forces.

Ingle et al. [36] investigated crater wear, which is a major contributor to tool wear. Tool wear was found to consist of chemical and mechanical wear components. Ingle [36] machined 1,045 steel with a cemented carbide tool and based on the work of Bhattacharyya and Ham [37] and Bhattacharyya et al. [38] an expression was developed to estimate the amount of tungsten transported by diffusion during the cutting time

$$W = 1.1284 C_o.F.t.(D/\tau)^{1/2}(\text{contact area}) \qquad (27)$$

Where W is the amount of tungsten dissolved, C_o is the equilibrium concentration of W at interface, F is the ferrite volume fraction, t is the cutting time, D is the diffusivity of tungsten in ferrite, and τ is the tool chip contact time. It was found if a tungsten carbide tool was used to machine steel, a TiN coating decreased the thermodynamic potential for dissolution by six orders of magnitude compared to the uncoated case. In related work Subramanian et al. [39] found that during chip deformation, thermoplastic shear occurs, causing localized heating which is sufficient to allow diffusion of tool material into the chip. Naerheim and Trent [40] observed this effect; it was found a concentration gradient of tool material was present across the chip thickness. Subramanian [36] found that a 10 μm thick, CVD deposited, HfN coating, resulted in less diffusion at higher cutting speeds; therefore, it was concluded that dissolution can be prevented by using a coating with a barrier that has the least thermodynamic potential for dissolution. During testing of the HfN coated tools, Subramanian [39] employed a technique suggested by Hastings et al. [41] that was based on a method developed by Boothroyd [42] to determine the chip temperature. Coated tools were found to be 75 °C lower in temperature and regardless of cutting speed it was found that HfN coatings prevented dissolution crater wear. As an alternative to coating tools, Subramanian [36] also investigated the effectiveness of adding inclusions into the workpiece; it was found that inclusions were only effective at preventing diffusion wear at low cutting speeds.

Sherby et al. [43] investigated deformation and diffusion of metals since they are important factors in cutting; it was found that the creep resistance of a metal is higher than its self-diffusion activation energy. Sherby [44] found during high temperature deformation that the formation of sub boundaries, grain boundary shear, and fine slip are related to interatomic diffusion. These static diffusion studies by Sherby [44] can help explain the dynamic diffusion present at the tool-chip interface. Gregory [45] studied diffusion between a cemented carbide tool and a workpiece Armco iron. Specimens were diffused under vacuum at temperatures of 1100, 1175, 1250 and 1325°C. These results were compared to a tool subjected to orthogonal cutting of the Armco iron. The experiment showed the first stage of diffusion is the outward migration of the cobalt binder phase followed by a buildup of titanium carbide in the reaction zone. The activation energy triggering cobalt diffusion was found to be 83 kcal/mole, which is comparable to 72.9 kcal/mole determined in another study by Suzuoka [46]. After the diffusion phase, a period of stable of wear sets in; this can be considered comparable to the run-in of a tool. Trent [47] suggests the stable period of wear is due to the presence of titanium carbide, which is more difficult to take into solution in steel compared to tungsten carbide. Gregory [45] argued that final wear is explained by the continual diffusion of the titanium carbide zone. The activation energies of its constituents are, 98 kcal/mol for the inward diffusion of iron, 142 kcal/mol for the volume diffusion of iron in tungsten and 77.5 kcal/mol for the outward diffusion of tungsten during the whole process. These energies point towards grain boundary diffusion mechanisms because Danneberg [48] found that the volume self-diffusion energy of tungsten was between 110 kcal/mol and 121 kcal/mol.

Nayak and Cook [49] reviewed some thermally activated models of tool wear. It was found models attempting to predict thermally triggered wear mechanisms using continuum diffusion theory experience difficulties when the workpiece material and tool material are similar e.g., cutting steels with high-speed tool steel. This is because the model assumes diffusion is driven by the concentration gradient of the diffusing material; hence there is a problem when this gradient is small. An alternate approach is to assume that vacancy concentration is the mechanism driving diffusion. This approach is successful in describing the wear of cemented tungsten carbide cobalt tools. Another approach is to explain wear as the result of a loss of hardness due outward diffusion of interstitial atoms into the chip; however, this method is in poor agreement with experiment. Consideration was given to a number of thermally activated processes that could trigger wear. A creep model could explain the triggering of wear, but the wear rate is calculated at 4×10^{-12}in/s, which is six orders of magnitude less than that observed; this trigger mechanism is therefore rejected. Depletion of interstitial carbon atoms in the tool was also investigated; if this mechanism were active a loss of hardness leading to wear by plastic flow would be observed. Diffusion couples were constructed to test for this possibility; it was found diffusion occurred in the opposite direction to produce a loss in workpiece hardness, therefore, this mechanism is rejected. Tempering wear was also considered. At critical temperatures transition carbides such as W_2C and Mo_2C can form, thus reducing tool hardness due to tempering. The equation, $V = 1.4 \times 10^{15}e^{-90000/RT}$ in/s can be derived for the tool wear rate, V, which agrees reasonably well with experiments conducted. R is the universal gas constant (cal/mol) and T is the absolute temperature (Kelvin). However, equations predicting other machining parameters generated during the derivation of this equation predict significantly different results to those observed. Atomic wear was also investigated, the energy required for atomic wear is provided thermally rather than mechanically by the applied stresses. Because tool atoms are in a higher energy state than chip atoms, overall migration is from the tool to the chip, where atoms flow to low energy sites in the chip such as vacancies, this is often observed. In this case the tool-chip interface is considered similar to a grain boundary interface and therefore the activation energy Q required for an atomic jump is also similar; this is in the order of 40–45 kcal/mol as determined by Leymonie and Lacomb [50].

A statistical model for the predictions of tool wear can be developed; Davis [51] suggested a Monte Carlo approach for modeling mechanical wear based on the following assumptions. Quanta of energy are added to the rubbing particles all of which leave the same site. The quanta of energy are added to the particles at domains randomly spaced over the surface at moments randomly spaced in time. The quantized energy of a particle diffuses continuously over the surface and into the material at a known rate. Finally, the surface energy of a particle causes it to detach as a wear particle. Bhattacharya and Ham [37] extended this approach developing expressions for the width of flank wear due to mechanical wear from abrasive and adhesive sources. The width of the flank wear, $H_f\,(T)$, at time T is given by:

$$H_f\,(T) = KV_C T^{1-\alpha} \tag{28}$$

Where V_c is the cutting velocity, T is the cutting time, α is the clearance angle, and K'' is given by:

$$K = 3K'/2C'(1 - \alpha) \tag{29}$$

Where C' is given by:

$$C' = C^2/\sigma^2 \tag{30}$$

Where C is a constant, σ is the deviation from the mean and K' is given by:

$$K' = \frac{Km}{\rho\left(\frac{\text{Tan}\,\alpha}{1-\text{Tan}\,\gamma\,\text{Tan}\,\alpha}\right)} \tag{31}$$

Where ρ is the density of tool material, m is the mass of the wear particle, γ is the true rake angle, and K is the constant governing decay rate. The height of flank wear, h_f, can also be determined by:

$$h_f = \left[\frac{5}{4}\frac{KV_c A^{\frac{3}{4}}(1 - \text{Tan}\,\alpha\,\text{Tan}\,\gamma\,)T}{b\text{Tan}\,\alpha}\right] \tag{32}$$

Where A is a constant based on the tool and workpiece combination and b is the width of cut. This model worked well when applied to experiments conducted by Bhattacharyya et al. [38] and experiments conducted by the internationally recognized tool wear collection body OECD/CIRP. However, a large amount of data for specific tool and workpiece combinations must be collected before this approach can be used. Bhattacharyya [38] points out the reliability of the model depend on the accuracy of the data, making this approach susceptible to compounded errors. It is also inconvenient to collect a large amount of data every time new machining conditions are encountered.

Tool coatings

In environments where tools experience high wear forces, and extreme pressure, sintered tungsten carbide tools are used. However, ultra hard materials such as silicon alloys, abrasive materials, synthetic materials and composites can only be machined with diamond-coated tools. Faure et al. [52] discussed the main issues involving diamond coatings. Thin uncoated tool edges are susceptible to rounding, and therefore, must be protected. Coating the tool with a hard material, such diamond, does not offer sufficient protection because there is a sharp hardness gradient between the tool and its coating. Upon impact, this gradient causes the coating to break, which in turn exposes the tool material and negates any benefits offered by the coating. This effect can be reduced if an interlayer is introduced with a hardness between the tool material and diamond e.g., TiC/TiN. Such an interlayer reduces the severity of the hardness gradient and enhances adhesion between the diamond and tool. Diamond is an ideal coating material; it has high hardness, high wear resistance and is chemically inert. However, it is difficult to coat steels, Ni alloys, cemented carbides and alloys containing transition metals with diamond. It is possible to coat WC-Co, but the cobalt must be etched away or a diffusion barrier introduced Jackson et al. [53].

Faure [52] applied diamond coatings by depositing a TiN interlayer and a TiN/TiC multilayer after a cobalt etch, seeding then took place with a solution of diamond micro grains. A 1% methane in hydrogen gas is introduced, tantalum filaments heat the chamber to between 900 °C and 1,000 °C, carbon then grows on the tool as a diamond coating. The coating effectiveness was assessed by a 'Revetest' test device and drilling experiments. The 'Revetest' device applies a constant force to an indenter during constant velocity displacement of the sample and critical loads are identified by an acoustic signal. Drilling tests were performed at 69,000 rpm at 3.5 m/min with a 1 mm diameter drill. The critical load causing coating failure is a function of the substrate hardness and the strength of adhesion between the coating and tool. It is common for a soft substrate to deform plastically while its coating does not, this is because it has a high Young's modulus. The coating alone bears the load and fails, thus exposing tool material to the usual wear mechanisms. Diamond coatings with a TiN interlayer can withstand 10 times more force compared to diamond coatings without the interlayer; adhesion is not necessarily better but toughness is improved. Both cases with and without an interlayer offer a significant improvement over tools with no coating, where rounding of the tool edge is observed after drilling only one hole. If a TiC/TiN/TiC multilayer is applied, large forces, around 100 N, are required to break the surface. It was found that thicker interlayers improved adhesion between the tool and diamond coating and interlayers with a Young's modulus between that of the tool and diamond coating produced better machining results. Uncoated tools were able to drill 10,000 holes and coated tools drilled 20,000 holes before they were considered worn.

Bell [54] categorized tool materials into three basic groups, high-speed steels, cemented carbides and ceramic and super hard materials including alumina based composites, sialons, diamond and cubic boron nitride. In addition the surface properties of these materials can be modified with the coatings varying in thickness between 10^{-1} and 10^{4} μm. Tool wear can be defined by the following, 'A cutting tool is considered to have failed when it has worn sufficiently that dimensional tolerance or surface roughness are impaired or when there is catastrophic tool failure or impending catastrophic tool failure.' To prevent failure Mills [55] suggests surface modification techniques, such as coatings, should be employed to improve tool performance. PVD (physical vapor deposition) has the capability to deposit wear resistant ceramic layers on high-speed tool steels, and it is increasingly being discovered that cubic boron nitride is an effective tool coating. Different machining processes are characterized by different wear mechanisms and the choice of tool coating should be selected to offer the best protection for a particular set of machining conditions; e.g., the combination of wear rate, bearing pressure and tool material.

Tool coatings can modify contact conditions, this alters the coefficient of friction, in turn, this alters heat generation and heat flow; there are four main types of coating. The first category is titanium-based coatings such as TiAlN; other elements are added to improve hardness and oxidation resistance. Titanium based coatings are popular because they provide a wide range of average protection properties, have good adhesion and are relatively easy and quick to coat. The second category is ceramic-based coatings, e.g., Al_2O_3, except for this example ceramics have good thermal properties and excellent resistance to wear but are difficult to deposit. The third category is super hard coatings such as CVD (chemical vapor deposition) diamond. The fourth category is solid lubricant coatings such as amorphous metal carbon, Me–C:H. Combinations of these coatings can give the best wear resistance; for example a recent development has been to take super hard coatings and deposit low friction MoS_2 or pure carbon on their surface.

Kubaschewski and Alcock [56] concluded that to prevent the onset of diffusion the enthalpy of the coating must be as negative as possible to increase the temperature at which diffusion is triggered. From this point of view most carbide coating materials such as TiC, HfC, ZrC are more suitable for cutting steel than tungsten carbide, similarly for the nitrides except CrN up to a temperature of 1,500 °C. The technique used to coat the tool can affect its performance; CVD requires high temperatures that can have an annealing effect on the tool. This affects the tool's toughness and rupture strength because there is a brittle η phase. A standard CVD process operating at 1,100 °C can reduce the materials strength by 30%. PVD techniques such as evaporation, sputtering and ion plating usually take place between 200 °C and 500 °C avoiding such problems.

Klocke and Krieg [57] summarized the three main advantages of multilayer coatings: (1) Multilayer coatings have better adhesion to the tool. (2) Multilayer coatings have improved mechanical properties such as hardness and toughness. (3) Each level of a multilayer coating can provide a different function. It is possible for coatings to protect the substrate from heat if they are good insulators, have low thermal conductivity and a low coefficient of heat transfer. An example of such an improvement is a TiN–NaCl multilayer, which has hardness that is 1.6 times greater than a single TiN layer. (Al,Ti)N coated tools have been compared to TiN and uncoated tools when milling at 600 m/min. The (Al,Ti)N coated tools exhibit much less flank wear, which correlates to a higher hardness, 2720HV for (Al,Ti)N versus 1930HV for TiN and an improved oxidation temperature, 840 °C for (Al,Ti)N v 620 °C for TiN. The improved performance of (Al,Ti)N coated tools is due to their ability to maintain a higher hardness at elevated temperatures.

It has been noted that in some extreme cases of machining highly abrasive materials, e.g., alloys containing highly abrasive Si particles, variations of Ti based coatings do not improve the tool life. In such cases only the hardness of diamond coatings can improve the abrasion resistance and therefore prolong tool life. Quinto et al. [58] investigated coatings deposited by CVD and PVD techniques on two alloys. Coatings with an Al content tend to perform better regardless of the application, coating process or chemical content of other the coating constituents. This is because abrasive resistance, oxidation resistance and hardness are all improved. Thermal relief experienced by the substrate is of special interest in terms of volume effects like fatigue and diffusion. Examples of coatings offering this relief are PVD (Ti,Al)N and CVD TiC–Al_2O_3–TiN. Quinto [58] found that PVD coatings outperform CVD coatings, which outperform uncoated tools.

Dry machining of steels in the range of 55–62HRC at 15,000–25,000 rpm generates cutting temperatures of 1,000 °C and the tool must be protected from oxidation wear. Previous work by Munz et al. [59] has shown that above 800 °C diffusion of stainless steel is triggered and cavities begin to form between the substrate and coating, although this can be prevented by adding 1% yttrium. One method used by Constable et al. [60] to analyze the integrity of coatings is Raman microscopy. It has been used to study wear, wear debris, stress, oxidation, and the structure of single layer PVD coatings; it is now being used to study multilayer PVD coatings. Constable et al. [61] demonstrated the usefulness of Raman microscopy when a PVD combined cathodic arc/unbalanced magnetron deposition system was used to coat high-speed steel and stainless steel for abrasion tests. The coatings had a thickness between 2.5 μm and 4 μm with a surface roughness of 0.02–0.03 μm Ra (roughness average). Polycrystalline corundum with a hardness of 1900HVN and Ra of 0.2 μm was brought into contact with the PVD coating. A constant load of 5N was applied and the relative sliding speed between the two surfaces was 10 cm/s. A 25 mW HeNe laser with an excitation wavelength of 632.8 nm was used to obtain Raman results. The PVD coating consisted of a 1.5 μm thick TiN base layer with alternating layers of TiCN, 0.4 μm thick and TiN, 0.6 μm thick; the total thickness was 3.9 μm. The expected wear debris was rutile, but anatase debris was also observed. This would suggest the contact temperature was lower than expected, indicating the coefficient of friction was also lower than expected. Deeming et al. [62] investigated the effect different coatings have on delaying the onset of oxidation. During high speed machining, temperatures regularly exceed 900 °C. Deeming found a TiN coating delayed oxidation until 500 °C, a TiAlN coating delayed oxidation until 700 °C and a multilayered system delayed oxidation formation until 950 °C. The final coating tested was TiAlCrYN, prior to deposition Cr is added to etch the tool, thereby achieving the smoothest most strongly adhered and dense coatings possible. The addition of yttrium increases wear at low temperatures, however at higher temperatures yttrium causes maximum wear to occur at 600 °C and minimal wear to occur at 900 °C. Without the addition of yttrium the wear rate continually increases with temperature. Deeming [62] suggests yttrium diffuses into the grain boundaries and at high temperatures there is some stress relaxation. Heat-treated TiAlCrYN therefore has lower internal compressive stresses than regular TiAlCrYN. TiAlCrYN also has a lower coefficient of friction with increasing temperature compared to TiN and also has a lower wear rate at elevated temperatures.

A problem with PVD coatings identified by Creasey et al. [63] is when ion etching is used to evaporate target materials; subsequent deposition by magnetron sputtering can lead to the formation of droplets, which adhere badly to the surface and cause weaknesses in the coating. This is particularly problematic when depositing TiAlN. It has been shown by Munz et al. [64] the melting temperature of the cathode material influences the number and size of these droplets. It was found generally that higher melting point targets reduce the number and size of defects when depositing TiAlN with UBM. An alternative to the Ti based coatings is CrN, which can be deposited at temperatures as low as 200 °C, the oxidation temperature for this coating is 700 °C. There are a number of ways to deposit this coating in addition to magnetron sputtering. Gahlin et al. [65] have demonstrated cathodic arc deposition and Wang and Oki [66] have demonstrated low voltage beam evaporation deposition. Hurkmans et al. [67] deposited the coating using a combined steered arc/unbalanced magnetron-arc bond sputtering (ABS) technique which helped overcome low adhesion problems id a pre-etch at 1,200 eV Cr prior to deposition; this can more than double the adhesive force from 25N to 50N. Wadsworth et al. [68] found that TiAlN and CrN coatings exhibit good properties for dry high temperature machining; and reasoned that a multilayer composed of these materials would produce an optimal coating for preventing tool wear.

Smith et al. [69] coated substrates with TiAl and TiAlN and performed a series of tests. Before coating a Cr etch was performed for the TiALN coatings, the growth defects previously discussed were highlighted in the TiAl coating, while the Cr etched TiAlN coatings was defect free. One of the substrates chosen for coating were a batch of drills, they were initially tested at 835 rpm and a feed rate of 0.28 mm/rev, for each subsequent test the spindle speed was incrementally increased. It was found TiAlN coated drills out performed commercially available drills. This improvement was partially attributed to the smoother surface observed on the TiAlN coating, in part a result of the Cr etch.

Petrov et al. [70] also experienced the droplet formation described by Munz during the coating procedure. Polycrystalline TiAlN alloys and TiAlN/TiNbN multilayers exhibiting smooth flat layers up to a total film thickness of 3 μm, were grown on ferritic b.c.c. stainless steel substrates at temperatures around 450 °C using unbalanced magnetron sputtering and cathodic arc deposition. Under UBM deposition conditions the films exhibited columnar growth with a compressive stress around 3 GPa. Compressive stresses are advantageous in tool coatings. For example, a built up edge welds to the tool's surface, when it reaches a critical size it is sheared off pulling tool material with it. The compressive forces within the coating resist such forces and maintain the integrity of the tool.

Salagean et al. [71] also deposited such coatings using an arc and unbalanced magnetron-sputtering cathode equipped with an Nb target. It was found prior etching treatment defined the quality of the new surface as well as the voltage bias during deposition. Donohue et al. [72] preferred TiAlN to TiN coatings due to their greater resistance of oxidation. Donohue [72] also found TiAlCrYN deposited by magnetron sputtering offered even greater oxidation resistance due to the additional alloying elements. Prior to coating, the samples were Cr ion etched. A 0.2 μm thick TiAlCrN base layer was then deposited, three separate coatings were grown on top of this layer to a thickness of 3 μm; they were TiAlCrYN, TiAlCrN and TiAlN. The hardness of the coatings was found to be TiAlCrYN = $HK_{0.025}$ = 2700 kgmm^2, TiAlCrN = $HK_{0.025}$ = 2,400 kgmm^2 and TiAlN = $HK_{0.025}$ = 2,400 kgmm^2. The oxidation temperatures were, TiN = 600 °C, TiAlN = 870 °C, TiAlCrN = 920 °C and TiAlCrYN = 950 °C. It was therefore concluded the extra alloying elements were significant. Annealing of the TiAlCrN at 950 °C for 1 h showed significant oxidation with micron sized voids, however, annealing of the same conditions of the TiAlCrYN shows no effect, highlighting the importance of yttrium.

Micromachining

Inamura et al. [73] found it difficult to apply finite element analysis to micromachining processes around 1 nm; they found molecular dynamics simulations to be more successful. The simulations highlight the need for a sharp tool because the model shows dull tools produce a large shear area; this leads to significant work hardening of the workpiece. It has been shown low forces are imparted to sharp tools operated at high cutting speeds, Kim and Moon [74] however, found that machining with a blunt tool the forces generated at high speed are significantly higher than forces produced by a blunt or sharp tool at low speed. It has also been shown that faster cutting speeds produce thinner chips. Therefore, if the full advantages of micro cutting, predicted by molecular dynamics simulations, are to be realized, maintaining a sharp tool is critical; this highlights the impotence of reducing tool wear by using tool coatings at the micro scale.

Burr formation is observed at the macro scale and can contribute up to 30% of the time and cost it takes to produce a part. Burr formation is also observed at the micro scale, but Gillespie [75] discovered macro scale burr removal techniques cannot be applied, because dimensional inaccuracies and residual stresses are induced. Smaller burrs are more difficult to deal with; therefore, investigations into burr formation and burr reduction have been undertaken. Gillespie and Blotter [76] stated there are three generally accepted burr formation mechanisms: lateral deformation, chip bending and chip tearing. Most micro burr formation studies are disadvantaged by operating well below the recommended cutting speed, for example, machining aluminum with a 50 μm tool demands a cutting speed of 105 m/min which would require a spindle speed of 670,000 rpm. At the macro scale, once formed, burrs can be quantified; for example burr height and burr width can be measured. However, at the micro scale such techniques are difficult to employ; if they are employed then Kim [77] found accuracy and repeatability are lost. Work by Ko and Dornfield [78] described a three-stage process for the burr, initiation, development and formation. They developed a model for the burr formation mechanism that worked well for ductile materials such as Al and Cu. However, at this time burr formation is not well understood, but correlations between machining parameters can identify key variables that help reduce burr size; for example observations of burrs by Lee and Dornfeld [79], indicate that up-milling generally creates smaller burrs than down milling.

It cannot be assumed that if milling is scaled down to the micro level, then machining characteristics will scale by the same amount. During macro machining, the feed per tooth is larger than the cutting edge radius; however, during micro machining the feed per tooth is equal to or less than the cutting edge radius. Therefore, a chip may not be formed and since the workpiece is still advancing, the tool may bend or fracture. Conventional milling tools have a slenderness ratio that prevents bending, whereas micro tools are susceptible to bending because their diameters are only a few hundred microns. During macro scale cutting the leading tool edge encounters bulk material and therefore avoids contact with hard particles; in comparison a micro cutting edge encounters individual grains ensuring contact with hard particles. This is related to the size effect described by Shaw [10]; the effect occurs because materials approach their theoretical strength as the scale decreases, this is because there is less chance of encountering material defects such as dislocations. Therefore, materials that are easy to machine at the macro scale could become difficult to machine at the micro scale.

The results of Ikawa et al. [80] suggest there is a critical minimum depth of cut, below which chips do not form. The analysis of Yuan et al. [81] indicates chip formation is not possible if the depth of cut is less than 20–40% of the cutting edge radius. Micro machining at 80,000 rpm produces chips similar to those created by macro scale machining, where chip curl and helix effects are observed, Kim et al. [82]. Kim also observes if the feed rate is too low a chip is not necessarily formed by each revolution of the tool. This anomaly can be demonstrated by calculation; if the feed rate is low enough, the volume of material removed is predicted to be greater than the volume of chips created. Thus some rotations that were assumed to create a chip could not have done so. This effect can also be demonstrated by experimental evidence. Sutherland and Babin [83] found that feed, or machining marks, are separated by a spacing equal to the maximum uncut chip thickness. Results show at small feeds per tooth the distance between feed marks is larger than the uncut chip thickness indicating no chip has been formed. Kim [82] concludes that a tool rotation without the formation of a chip is due to the combined effects between the ratio of cutting edge radius to feed per tooth and the lack of rigidity tool of the tool.

Work by Ikawa et al. [84] and Mizumoto et al. [85] showed single point diamond turning can machine surface roughnesses to a tolerance of 1 nm; the critical parameter was found to be repeatability of the depth of cut. It is useful to model the cutting process using a molecular dynamics simulation approach. Mizumoto's simulations [85] suggest the depth of cut and cutting edge radius are critical parameters that determine chip formation. By dividing the process into small intervals it is possible to compute the position of each atom, in this way material flow during the chip formation process can be predicted. Because a molecular dynamics simulation does not account for electron behavior temperature predictions are unreliable.

Shimada et al. [86] compared their predicted simulation results with experimentally determined values for a copper workpiece and found them to be accurate. If the strain applied to the workpiece by the tool is large enough, forced lattice rearrangement will occur therefore generating dislocations. As the tool advances more dislocations form at the tool-chip interface and if enough join at the primary shear zone then a chip is formed. After the tool has finished cutting, dislocations that penetrated the workpiece migrate out towards the surface because the lattice can relax. This phenomenon can be observed as atomic sized steps on the surface, this represents the best surface roughness possible Shimada [87].

In macro scale machining the tool edge sees bulk properties of the workpiece, however, in micromachining the tool edge sees features of the material matrix such as grain boundaries. Shimada et al. [88] used molecular dynamic models to simulate this interaction. Simulations running cutting speeds at 2,000 m/s show the kinetic energy imparted to the workpiece is far greater than the cohesive energy of the workpiece. Other molecular dynamic simulations have identified four stages of the cutting process: (1) Compression of the work material ahead of the tool; (2) Chip formation; (3) Side flow; and (4) Subsurface deformation of the workpiece. Komanduri et al. [89] conducted simulations that help highlight differences between macro and micro scale cutting, for example a volume change is observed when machining silicon. A pressure induced phase change modifies the structure from cubic to body centered tetragonal resulting in a 23% denser chip. Usually silicon is brittle but if more electrons are available in the conduction band (usually at high temperatures), it can behave more like a metal. It has also been reported that there are no dislocations found in silicon substrates, this is an attributed effect of cutting at high speed. Significant differences were found between the machining characteristics of aluminum and silicon; most of these were attributed to the variation in ductility between the two materials. Aluminum chips form due to plastic deformation along their preferred crystallographic planes. The mechanism for plastic deformation of silicon is similar when it is being machined or extruded; this is due to the phase transformation from cubic to body centered tetragonal. This phase change also causes surface and subsurface material below the chip to become denser. The evidence for phase transformation is that silicon, which is usually brittle, acts in a more ductile manor; this is only made possible by a pressure induced phase change. Komanduri [89] also suggested subsurface damage, although very small, is inherent in micromachining; however, the subsurface damage was found to decrease as the aspect ratio of the tool decreases. The subsurface densification was shown to decrease with an increase in rake angle and increase with an increasing aspect ratio. Side flow was predicted to decrease with increasing width of cut and increase with an increasing depth of cut.

Low cutting forces are produced at the micro scale therefore smaller machine structures can achieve the damping and stiffness characteristics required for successful machining. The smaller footprint allows a greater number of machines per floor area yielding a greater throughput and making the technique suitable for mass production. Vogler et al. [90] have also examined the differences between macro and micro scale machining; it was observed the tool edge and workpiece material grains become comparable in size. The tool edge radius becomes a similar size to the uncut chip thickness; this results in large ploughing forces. The tool's slenderness ratio reaches a point where tool stiffness is reduced. Vogler's force prediction model accounted for different grains such as pearlite and ferrite by examining their individual machining characteristics and incorporating them into his model, machining characteristics of the bulk material can then be predicted. It was found cutting edge conditions had a large effect on machining forces. A worn tool edge can produce 300% more cutting force and results in poor surface roughness and increased burr formation.

Meso machine tool design

If high speed machining is to be successful at the micro and nanoscale, high spindles speeds must be employed to ensure materials are processed at their recommended cutting speed. Popoli [91] has considered design problems of high-speed spindles and the limitations of adapting current spindle design. Conventionally belted or integral motors provide the power source for spindles. Conventionally belted motors rely on friction; ultimately this becomes a limiting factor at high speed due to the generation of high temperatures, therefore, integral motors are the only option.

The next design issue to consider is the choice of bearings, which are required to stabilize the shaft during machining. Conventional high-speed bearings are limited to a speed of around 100,000 rpm. The fastest rated bearings commercially available are precision bearings, usually used for dental applications, their maximum speeds are rated between 400,000 rpm and 500,000 rpm. They can only hold small diameter shafts, around 3 mm, and the lateral forces must be low during operation. The maximum speed of precision bearings is ultimately limited by friction and the use of non-contact solutions such as air bearings must be considered. Air bearings however, are sensitive to external debris such as dust but high positive pressures usually prevent this from being problematic.

Considering current high speed motors Frederickson and Grimes [92] highlight a problem of rating spindles by quoting the power, which is the product of torque and speed. A high power motor could be the result of low torque and high speed or high torque and low speed; and maximum power may not be available at the maximum speed. Therefore, quoting power yields little information about the motor. Conventional wisdom dictates spindle design should focus on increasing torque to provide more cutting force; this may not be true at the micro scale, because if the spindle speed is high enough, predictions have shown cutting forces decrease. Electric motors work when current is applied to the windings, which briefly turn magnetic; permanent magnets repel the coil rotating the spindle. Larger windings and magnets produce more torque therefore torque is proportional to motor size. The bulk of a high torque motor can be reduced by careful design, for example a 100 mm diameter stator has 3 Nm of torque if its length is 40 mm, but 15 Nm for a length of 150 mm. Elongating the spindle length can have a detrimental effect on machining performance. The effect of increasing distance between bearings is to lower the spindles natural frequency closer to its operating speed. The shaft then becomes susceptible to increased vibrations and bending. For example a 10 mm elongation in motor length can alter the natural frequency from 51,000 rpm to 44,445 rpm, the operating speed of the example motor is 40,000 rpm, which is within 5,000 rpm of the natural frequency. A greater difference in rpm would be preferable reducing the risk of operating at the natural frequency. Spindles often run hot; there are two methods of reducing temperature, a liquid cooling jacket or cooling fins. Both techniques draw heat away from the spindle housing; the liquid method is more compact because fins are not required.

Often recommended micro scale cutting speeds can require a spindle speeds well in excess of 500,000 rpm. Current dental drills can reach speeds up to 500,000 rpm but have a run out of 10 μm; a figure which is usually greater than the chip thickness. Tools with diameters of 25 μm have been used to mill at 30,000 rpm but can only achieve feeds of 5–14 inches per hour. Tool wear is different at the macro and micro scales. At the macro scale tools usually are considered worn due to edge wear, at the micro scale they fail because bending strength is exceeded. This occurs when the chip thickness ratio is larger than the tools edge radius and cutting forces are large. If chip thickness is smaller than edge radius the result is a negative rake angle (up to 50°), thus increasing cutting forces required to form the chip and highlighting the need for smaller chips. Therefore, Zelinski [93] has concluded the only way to achieve a reasonable material removal rate is to rotate the tool faster. There are no established methods for concentrically holding the tool at the micro scale. Therefore, Zelinski [93] experimented with securing the tool shaft on a bearing and rotated the tool using frictional contact on the tool shaft with a large diameter fast turning wheel. High speeds are achieved through extremely large gearing ratios between the large diameter wheel and small diameter tool. Tool breakage can be detected if cutting forces are monitored throughout the machining cycle. The problem of high-speed bearings has also been encountered in the design of MEMS components. It has been shown rotating micro devices must have similar tip seeds to their macro scale counterparts. For example, macro scale turbo machinery typically has tip speeds around 500 m/s but current MEMS tip speeds are limited by MEMS bearings to approximately 2 m/s. Frechette et al. [94] has considered this problem and designed a micro gas bearing. The rotor sits on a fluid film avoiding solid contact thereby minimizing friction. Gas bearings are used to support radial motion and gas thrust bearings support axial motion. The device was designed to overcome viscous drag produced by the gas bearings and fluid membrane; at 500 m/s this drag was computed to be 13 W. The radial bearing (or journal bearing) separates the rotor and housing, it is 300 μm deep and has an average separation of 15 μm, which is maintained by a pressure differential. If the rotor becomes dislodged the pressure differential restores its center position, which was first demonstrated by Orr [95]. However, the hydrostatic journal bearing acts like a spring and a certain speed coincides with the natural frequency of the rotor producing unwanted oscillations. Diagnostic equipment can detect the onset of such oscillations and the pressure can be altered to change the rotors natural frequency thereby avoiding the problem. Rotating micro devices such as these have reached 1,400,000 rpm before failure; this is equivalent to a tip speed of 300 m/s. Failure occurs when the device becomes unstable and crashes, instabilities results from imperfect manufacture of the rotor and stator system. It is therefore critical to manufacture rotor and stator components accurately so stable conditions can established to prolonged operation of the device. It was also discovered that after surpassing a certain critical high speed the body rotates about its center of mass rather than its geometrical center.

Future applications and research directions

Machining at high speed at the micro scale is certainly of interest; it is clear high spindle speeds, in excess of 500,000 rpm, must be achieved if mechanical micro machining is to be successful. Achieving these high spindle speeds is the first obstacle to overcome; novel techniques of stabilizing the machine, providing bearings capable of holding the shaft and methods of powering the shaft must be developed, since current macro scale techniques cannot be applied in the high speed micro scale regime. Possible solutions to these problems include using a terraform machine structure for stability, air bearings to support the shaft and air turbines to power the shaft. Subsequently, high speed micro machining should be possible.

Material removal is expected to differ slightly from macro scale machining, important effects to consider are the ratio edge radius of the tool and the uncut chip thickness, which may or may not cause the tool to bend; and the size effect, where the material approaches its theoretical strength and becomes more difficult to machine.

Finally tool wear is an important factor; the tool's dimensions must be maintained during machining; both to ensure accurate parts are created and the process is economically viable. Conventional tool coatings aim to reduce the cutting temperature and prevent wear by diffusion. However, at the micro scale the surface area of the chips is large, therefore, heat is removed away from the cutting zone rapidly and the temperature at the cutting zone may be low. Therefore, the conventional reasoning for using coated tools may not apply in the micro high-speed regime. The importance of diamond coatings may increase however, since the tool edge encounters individual grains that may be abrasive and diamond performs well when resisting wear by hardwearing asperities.

Overall the technology is in its infancy, but if all the aspects discussed above can be developed sufficiently, then high speed micro machining can become a productive micro manufacturing technique.

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