The Machining Dynamics and Mechanics Laboratory (MDML) at Michigan Technological University is an effort directed by Professor William (Bill) Endres to merge the domains of cutting mechanics and machining dynamics to realize an integrated approach to machine-tool, tooling and process analysis. While there are numerous researchers in the United States and abroad who specialize in machining processes, most concentrate their efforts in either cutting mechanics or machining dynamics. The MDML effort is one of only a few in the world with the breadth in expertise required for bringing the realities of real-process cutting mechanics to the machining dynamics problem.

Research outcomes are centered around meaningful measures of process performance, including part quality as defined by dimensional accuracy, surface finish, waviness, burring and residual stress, and other limits on productivity, such as chatter/instability. In other words, traditional focuses of force prediction and thermal modeling are viewed as a means to the end, not the end itself. With a focus on process performance, the MDML aims to conduct basic research motivated by realities of practice and a need to understand qualitatively, and predict quantitatively, the performance of machine-tool systems during their development, planning and diagnosis. Additionally, inspired by pressing challenges of industry practice, concepting of new tooling prototypes is an ongoing effort that is enabled by the findings of the group’s fundamental research. Much of the MDML’s research is transferred to practicing engineers through industry collaboration and short courses, as well as university instruction, including two university courses Professor Endres has developed in modeling and analysis of machining.

You are invited to visit the MDML website, via Professor Endres’ webpage (www.mtu.edu/~wjendres), for more information, electronic versions of the project summaries enclosed here, and access to full publications on these and related topics.
An Upper-Bound Model of Edge-Radius Effects on Machining Forces

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Sponsor: National Science Foundation (CAREER Grant DMI-9734147)

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Introduction

To achieve the edge strength needed to cut harder materials, slightly negative rake angles are used with a honed radius or chamfer applied to the cutting edge. Edge effects become important when the applied geometric feature is sizeable relative to the scale of the cut — the uncut chip thickness (feed). In finish cuts where the feed rate is low, even the “natural” sharpness of a cutting edge becomes sizeable relative to the uncut chip thickness. In these cases, appropriate model-based selection of the edge feature requires a model that explicitly accounts for the edge geometry rather than absorbing its qualitative effect into empirical parameters, as is the case to date in the most commonly used models.

The aim of this work is to develop a machining model that explicitly includes the effects of edge radius without resorting to the highly computational finite element method or the complexities of rigorous slip-line analysis. While it is presumed and/or known that the edge geometry affects surface finish, residual stress, cutting temperature and tool wear, the first step taken here is to model the effects of edge radius on forces. While the model achieved here is not fully predictive, it does provide a means to analyze force data to assess how well the model represents the effects of edge radius on flow stress via the edge radius’ effects on strain and strain rate. Continuing work aims to realize a fully predictive model.

Approach

The model is based on material separating at a stagnation or separation point on the cutting edge, with the material above that point forming the chip and the material below that point forming the machined surface. The geometry of the process is idealized to achieve a deformation zone and equivalent tool each made up of straight boundaries, as shown in Fig. 1.

The equivalent tool geometry is formulated as follows. The edge radius is approximated by two line segments, the vertex of which is the stagnation point — point P. The lower portion of the tool edge radius is replaced by an equivalent chamfer — line segment CP. The rake face and upper portion of the edge radius are replaced by an equivalent rake face — the line segment that connects point P to the point at which the chip leaves the tool. The separation point is located on the edge radius by the separation angle \( \theta \), which is ultimately an input. The value of \( \theta \) is chosen based on various past studies of critical negative rake angle — the extreme negative rake angle at which chip formation ceases. The equivalent rake face is characterized by its equivalent orthogonal rake angle. Supportive of this concept is visual experimental evidence, both in this work and others’ past work, that shows the chip to leave the tool at a direction consistent with a more negative than nominal rake angle.

The deformation zone is defined to be consistent with upper-bound analysis as follows. Plastic deformation initiates at the forward boundary AB, which is a plane of maximum shear stress; hence, AB meets the free surface at 45°. Plastic deformation concludes at the upper boundary AP and at the lower boundary BC. Material above point D, which is at the same level as point C, rises up to the separation point P. Material below point D is plastically deformed down to point B. Lines AB, BP and AP are lines of velocity discontinuity; Fig. 1 shows the velocities in the deformation zone that define the flow and ultimately the strain rates. Boundary AP is representative of the traditional shear plane; hence, it is oriented by the shear angle \( \psi \). Boundary BC is oriented by the angle \( \psi \), which then dictates the depth of plastic deformation (point B).

Given the process geometry, the forces are then determined under the premise of equilibrium. That is, that the machining force holds in equilibrium all elements of the system — the workpiece, deformation zone, chip and tool. Forces on a boundary are computed as the product of the normal and shear stresses on the boundary and the length (area) of the boundary. The boundary of choice for computing forces is the lower boundary of the deformation zone (line segments AB and BC). Figure 2 shows the process geometry split at this boundary. The normal pressures \( P_1 \) and \( P_2 \), and shear stresses \( S_1 \) and \( S_2 \), act equally and opposite on the workpiece and the deformation zone. Summing the resulting forces in the cutting and thrust directions yields the process forces.

To be predictive, however, one needs the values of \( P \) and \( S \), as well as all values defining the geometry. The normal pressure can be related to the flow stress \( S \), which can be empirically modeled in terms of strain and strain rate with power-law coefficients obtained from measured forces. A sensitivity analysis shows minimal sensitivity of forces to choices of \( \theta \) and \( \psi \). However, where this model lacks a “predictive” capability is the lack of a good model for the shear angle \( \phi \). Therefore, the model is used at present only as a means to analyze force data.

Results

Force data is analyzed given measured shear angle values by fitting a power-law model for \( S \) in terms of strain and strain rate. If the model is well correlated across edge radius, rake angle, and uncut chip thickness, it is considered internally consistent — that is, the model is consistently capturing the internal stress-strain-strain rate behavior across all geometry.

The model works well across uncut chip thickness and rake angle for sharp tools. However, large data sets across large ranges of edge radius show deficiencies in the model. Continuing work aims to address this issue by modeling the process geometry with curved boundaries that are more consistent with high-magnification images acquired in related work.

Benefits

- The simplicity of the model provides an analytical result that clearly shows effects of geometric parameters.
- However, inconsistencies in exercising the model justify and motivate a more sophisticated model, such as a slip-line field (SLF).
Introduction

Applying a honed radius to a cutting edge to protect it from chipping is being increasingly employed to enhance tool life. However, it is known that cutting efficiency drops significantly when the uncut chip thickness drops below about two times the edge radius. Inefficient cutting increases mechanical and thermal loads per unit material being removed. Therefore, when the uncut chip thickness is small, such as in hard machining, drilling and finish cuts, edge-chipping protection comes at the cost of increased forces and tool temperature. A better understanding these pros and cons is needed to appropriately size the edge radius to enhance the associated advantages.

For corner-rounded tools used in turning, boring and face milling, the uncut chip thickness reduces along the corner radius and ultimately approaches zero near the tip of the tool. Therefore, if an edge radius is applied to protect the cutting edge from chipping, the associated cutting inefficiency is exacerbated along the corner radius, in particular near the tool tip. An increase in corner radius further increases the region of low uncut chip thickness, making cutting near the tool tip even less efficient, presumably increasing edge temperature and tool wear. On the other hand, it is well known that tools with a near-zero corner radius (sharp-cornered tools) often exhibit an increase in wear that is concentrated near the sharp corner. Therefore, it was hypothesized that some degree of increase in corner radius could serve to spread the overall thermal load across a greater region of the cutting tooth, providing a better heat conduction path to the bulk of the tooth, potentially lowering the temperature along the cutting edge and reducing wear.

Approach

Uncoated plain carbide (ISO C3) TPG 43X inserts are used to cut 1059 steel bar-stock with a hardness of 58.5 – 61.3 Rρ. Three edge-radius levels (up-sharp (~5-10 µm), “small” (25 µm) and “large” (50 µm)) and four corner-radius levels (0.2, 0.8, 1.2 and 1.6 mm) are considered. Tests are conducted at three small feed rates (0.022, 0.037 and 0.083 mm/tooth) representative of those seen in hard machining and finish cuts. For each edge radius, each combination of feed and corner radius is replicated three times using the three edges of the same triangular insert, for a total of 36 wear tests at each edge radius. All tests are performed at 3.05 m/s, or 183 m/min, for a three-minute duration. The depth of cut is chosen to be 2.5 mm, making it at least three times the depth at which the lead edge transitions into the corner radius (per ISO standards), for all corner radii. Flank wear is measured on an optical microscope. Data are recorded as an average of three measurements at the general location on the lead edge beyond the depth of cut notch, and a single measurement at the tool tip. These are referred to as the “lead edge” and “corner-edge” measurements, respectively.

Results

The wear measurements are shown in the figure below. For the up-sharp tools, there is clearly a corner radius that minimizes flank wear. For the small-hone tools (not shown), the results are similar in terms of the trends. In terms of magnitude, wear is higher for the 25-µm edge radius. This is expected since the presence of a sizable edge radius decreases cutting efficiency, which subsequently increases overall tool temperature and wear rate. Both the up-sharp and small-hone tools exhibit slightly lower wear at the tool tip than on the lead edge. These data are consistent with the initial hypothesis that adding a corner radius will improve wear by better distributing the thermal load. The fact that wear eventually begins to increase with further increases in corner radius indicates that the chip thinning (increased thermal load) effect of increased corner radius eventually dominates its beneficial thermal-load distribution effect.

Based on the above, and intuition, one would expect any increase in edge radius to yield a reduced cutting efficiency and subsequent increases in temperature and wear rate. Given that expectation, the large-hone data initially appear inconsistent with wear levels being higher at some corner radii and lower at others. Apparently it is not that simple. Adding trend curves for the large-hone tools, as shown in the figure, highlights a similar trend of decreasing flank wear with an increase in corner radius, most dramatically and quickly for the lowest feed. From this perspective, since the wear cannot continually decrease, it either asymptotically reaches some level as corner radius continues to increase, or there must be a corner radius beyond the range studied that will minimize wear. In either case, these data motivate one to accompany a larger edge radius with a larger corner radius in order to decrease wear. From a physical point of view, the data demonstrate that the higher thermal loads that come from reduced cutting efficiency at a higher edge radius require a larger corner radius to effectively provide the thermal-load distribution effect seen for the sharper tools.

Benefits

- Flank wear can be minimized by simultaneously and properly selecting corner radius.
- Adding a corner radius to tools that traditionally have a sharp corner, such as end mills and drills, may reduce flank wear.
- When an edge radius is present, the wear-minimizing corner radius is larger, resulting in improved feed-groove finish.
The Basic Effects of Flank Wear on Forces for Edge-Radiused Tools

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Introduction
Machining research has yielded many studies of cutting tools that are sharp (no edge preparation), flat-faced (no chip control), and fresh (unworn). Some recent successes have been achieved for “fresh from the box” cutting tools that have either an edge preparation or chip control. However, these recent successes are limited in their practical utility since all cutting tools operate with some level of wear throughout their useful life. Simply put, industry practice requires a quantitative understanding of how a cutting tool’s performance evolves beyond the fresh-tool state. Furthermore, since most modern cutting tools are equipped with an edge preparation, understanding wear of sharp cutting tools is also limited in its utility. Despite the rarity of efforts to study tool wear in the presence of edge preparation, anecdotal evidence, intuition and limited published data suggest that edge preparation is closely linked to flank wear.

Predicting the change in cutting-tool performance as it wears would ultimately allow one to predict tool life — the point at which the cutting tool’s performance is no longer satisfactory and a tool change is needed. A step toward that ultimate goal is an understanding of how process mechanics are affected by the comprehensive edge condition — flank wear combined with edge preparation. To that end, presented here is

- a basic experimental study of flank wear in the presence of edge radius;
- an interpretation of data through a mechanics-driven empirical model as guidance in future development of a predictive mechanistic model.

Approach
Traditional tool wear testing employs OD turning of large bars to achieve the required long-duration cuts. The ISO standard specifies that the depth of cut (d) be two times the corner radius (rc) to avoid corner effects. However, a previous study of the investigators showed that, even when exceeding the ISO specification on d/rc, the corner radius has a significant effect on flank wear, and, furthermore, that this effect varies with edge radius. This motivates studying the fundamental mechanics of the wear problem for edge-radiused tools in the traditional manner — under straight-edged orthogonal conditions via tube-end turning. Unfortunately, this tried-and-true technique is not practical for tool-wear testing since

- each tube specimen permits only a single pass, since the cut consumes the entire wall thickness, and
- a tailstock cannot be used for such an arrangement, which limits the length of a work specimen to about twice the tube diameter.

Given the above, a two-tool setup, as shown in Fig. 1, was designed and fabricated. The grooving tool creates a short tube-like lip that is then removed by the main tool under straight-edged orthogonal conditions. A borescope is used to measure flank wear without disturbing (removing) the insert. Tests are conducted on 1040 steel at constant values of uncut chip thickness (h = 37 µm), cutting speed (366 m/min) and width of cut (3.18 mm). Levels of edge radius (r_c) ranging from up-sharp (~10 µm) to 125 µm provide a total of 15 cuts.

Results
Representative force evolution data are shown in Fig. 2. When the tool is blunt (h/r_c < 1), the force initially decreases with increasing wear-land length (l_w) before increasing, whereas when the tool is sharp (h/r_c > 1) the forces increases monotonically with l_w as commonly accepted for decades. The unexpected decrease in force with wear is explained by the sharpening of the blunt edge by the wear land. As such, the chip-removal force (F_cr) decreases as the edge sharpens making the chip-removal process more efficient, while the wear-land force (F_w) increases monotonically as the wear-land grows. The minimum force occurs when The geometry of a blunt tool is seen in the upper portion of Fig. 3 where the dashed lines show the original blunt tool and the wear land that forms. The minimum occurs when dF_w/dl_w = dF_cr/dl_w. This balance of gradients occurs when a small portion (15° to 25°) of the edge radius is still present.

For interpretive purposes, a mechanics-driven model is developed as F = F_cr + ∆F_w + F_w, where

- F_cr is the fresh-sharp force — the force for a perfectly sharp tool,
- ∆F_w = ∆F_cr l_w / (r_c) is the fresh-blunt force rise — the increase beyond F_cr that occurs due to an edge radius r_c — which is then adjusted to obtain
- ∆F_w, ∆F_cr = ∆F_w/∆l_w — the worn-blunt force rise, where ∆l_w = e^(W_lw) is a wear-sharpening factor and the non-dimensional wear-land length L_w = l_w/r_c (L_w corresponds to the complete edge being worn away), and
- F_w = e^(W_lw) is the wear-land force.

Using data at the start of each cut across all the edge radius levels yields F_cr and F_w. All data for which L_w ≥ L_w yields the coefficients of F_w. A complementary edge-sharpening test, as depicted in Fig. 3 (top), confirms the form proposed for ∆l_w (see bottom of Fig. 3). The model well represents the data across both l_w and r_c with peak errors of 10% and 20% in the cutting and thrust directions, respectively.

Benefits
- The knowledge compiled here provides substance to any further modeling of wear effects on force.
- Edge radius may be chosen to achieve a minimal change in force up to substantial wear levels.

Fig. 1 Two-tool wear-testing apparatus.

Fig. 2 Effect of flank wear on thrust force for sharp and blunt tools.

Fig. 3 Edge sharpening: tool & result.
Introduction
Owing to their high stiffness, dimensional stability, heat insulation, and excellent resistance to chemical erosion, ceramic materials and glasses are attractive for applications in the computer, automotive, aerospace and optics industries. Starting with net-shape sintered parts, grinding has been the process of choice for machining structural ceramics. Some research has explored the use of geometrically defined cutting tools to achieve the same damage-free results at material removal rates higher than those in grinding, yet still low by machining standards. The use of geometrically defined cutting tools at much higher material removal rates would
- promote wider-spread use of ceramics by making their use more cost effective, and
- avoid spatial variation in mechanical properties that is inherent to net-shape sintered parts and can present functionality problems.

The objective of this effort is to conduct basic orthogonal cutting tests from the ductile-regime that others have studied up to much higher material removal rates. The results are a first step toward assessing the possible use of a rough–semi-finish–finish strategy to machine ceramics from bulk stock. Soda-lime glass is chosen for this initial study due its well-defined fracture mechanics that result from a lack of grain structure. The simple process geometry considered avoids clouding of the results by effects of complex tooth geometry and process kinematics.

Approach
Tests are conducted on a tabletop planer that was designed and fabricated specifically for this study. It possesses high stiffness and resolution in setting the uncut chip thickness. Forces are measured with a piezoelectric dynamometer while in-situ video is recorded using a CCD camera viewing the process through a borescope. The cutting tool is synthetic single-crystal diamond, which provides a cutting edge that is more uniform and sharp than those of polycrystalline diamond tools. To avoid edge effects tests are conducted on segments of glass tube, as shown in Fig. 1. By ramping the workpiece in the cutting direction the uncut chip thickness and width of cut increase gradually as the cut progresses. The gradual change in uncut chip thickness allows transitions of machining modes to be observed and correlated to the actual uncut chip thickness, which is computed based on the width of cut and the tube radius.

Results
Figure 2 shows micrographs of the surface produced with a +5° rake angle. It shows two successive regions of the workpiece where the width of cut and uncut chip thickness are increasing from left to right.

The early stages of the cut is shown in Fig. 2 (left). At the start of the cut the target small uncut chip thickness is very small. Where the tool begins contact there is visible change in the appearance of the surface. This appearance is also seen to extend, as the cut progresses, along the outer edge of the width of cut. This is consistent with the reducing uncut chip thickness from the centerline out toward the edges of the cut, which results from the tube's curvature. Beyond this region is a region of distinctly different appearance. The small scuffmarks are seen in each region, yet there is some other change in appearance. The scuffmarks result from the roughness of tool's cutting edge scratching the workpiece surface. In the second region, however, material is being removed in a ductile fashion whereas in the first region no material removal takes place.

As the uncut chip thickness gets larger, another mode of cutting commences. This mode, shown in Fig. 2 (right), results in cracks growing down into the surface and ahead (toward the right) of the tool. The sub-surface cracks show up as reflected light from below the surface. Careful inspection, however, also shows a continuation of the edge scratching. Therefore, this surface here is still formed by ductile-regime chip removal. However, this region differs from that before it in that the cutting loads are high enough to cause brittle fracture in the surface. The loads, however, are not high enough until later in the cut, as seen also in Fig. 2 (right), to continue the cracks back up to the surface. When these cracks reach the surface a chink of material is spalled from the surface. The spalling continues as a series of half-clamshell shaped divots. The shape results from the cutting edge, after traversing one divot, making new contact with the intersection of two other divots creating a point load. Further evidence that material is removed in chinks is that the scratch marks are no longer present since there is not material for the edge to rub after chip formation.

To summarize, the results show
- Pure edge-rubbing, which imparts small-scale scratches on the surface.
- Damage-free ductile-mode chip formation, or ductile-mode machining, where a chip is formed at a small uncut chip thickness through plastic deformation of the workpiece. The new surface created by chip removal then experiences rubbing of the edge, which leaves behind its characteristic light scratches.
- Surface-damaged ductile-mode chip formation, or semi-ductile mode machining, which is identical in terms of chip formation to the mode above with the addition of surface cracking.
- Brittle-mode spalling, where the uncut chip thickness is too large for plastic deformation alone and the surface cracks continue to grow ahead of the tool until they rise back up to the free surface causing removal of a half-clamshell shaped chip/spall.

The effects of uncut chip thickness and rake angle are shown in Fig. 3, where it is confirmed that more negative rake angle tools allow increased material removal rate for a given surface damage regime. The three material removal modes can be considered rough cutting (brittle spalling), semi-finish cutting (surface cracking) and finish cutting (no cracking).

Benefits
- If damage-free ductile-mode chip formation can remove surface cracks without further propagating them, then a rough–semi-finish–finish strategy can be adopted for high productivity machining of bulk stock.
- Rake angle can be chosen to promote rough cutting or finish cutting.
Modeling Surface Damage in Glass Machining

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Introduction
Given the increasing demand for components made of brittle materials, more productive means of machining them are sought. Using a geometrically defined cutting edge (machining) rather than an abrasive tool (grinding) can improve productivity. Still, to realize fully ductile material removal to avoid surface damage, the uncut chip thickness must remain about one micron or less. While this provides much greater productivity than grinding, the material removal rate is still low. To provide even higher productivity when machining brittle materials, a rough–semi–finish–finish machining sequence is proposed for further study based on a related experimental study of the authors. This strategy would allow surface damage to occur at a level that is acceptable for the particular stage in the process.

The orthogonal cutting experiments noted showed that when glass is cut with an uncut chip thickness slightly beyond the range of the ductile regime, an array of surface cracks begins to appear. Increasing uncut chip thickness results in an array of divots produced by spalling of chips from the surface. The level that is acceptable at a particular stage of the process is quantified by the need to avoid surface damage that extends to a point beyond the final target surface. This work aims to model surface damage for given process forces as a first step to give guidance in selecting what to transition from the rough, to semi-finish to finish stages.

Approach
For the spalling case, a simplified analytical model is developed based on past work on brittle film de-lamination and spalling. The model provides insight to the trends with respect to process load direction and magnitude. For a more refined result, finite element analysis is employed with both manual crack propagation via re-meshing and automatic crack propagation via mesh design. The finite element models are used in a computer-based experiment to formulate a closed-form relation between process load and crack geometry, such as length ahead of the tool and, most importantly, the maximum depth below the tool.

Results
Figure 1 (top) shows the finite element meshes for the spalling analysis, the left figure being for higher cutting (horizontal) force divided by modulus ($F/C/E$) and the right figure being for lower $F/C/E$. These images show that as either force increases or the elastic modulus decreases, the length of the spall decreases. This dependence on force and modulus comes about due to the geometric nonlinearity that exists, by which as $F/C/E$ increases the chip bends upward more per length of spall as it grows. As the chip bends upward, the moment at the crack tip increases ultimately causing the crack to deviate up to the surface forming the spall.

From the perspective of a rough–semi–finish–finish strategy, more important than spall length is spall depth. Spall depth is graphed in Fig. 1 (bottom) versus normalized fracture toughness (proportional to $K_i/E$), which dictates in part the force required to grow the crack. These results show that as $K_i$ (related to $F/C$) increases or $E$ increases, which cause the spall length to decrease, also cause the spall depth to decrease.

Figure 2 (top left) shows the finite element model for the surface cracking analysis. Shown here is the mesh designed to allow a more automated analysis than did the manual re-meshing seen in Fig 1 (top). The mesh design was studied and validated by comparison to the chip spalling results. The square root of the normalized crack depth versus normalized cutting force ($F/C/K_i$) is graphed in Fig. 2 (bottom). Again the crack depth increases with load in a square-root fashion. The load ratio ($\lambda = F/C/F_T$) plays an important role as seen. As the thrust (vertical) force component increases with respect to the cutting component the crack depth reduces. This is consistent with experimental results that show reduced propensity for cracking at more negative rake angles; more negative rake angles are known to decrease load ratio.

Whereas the results shown in Fig. 2 (bottom) are for point application of $F_C$ and $F_T$, Fig. 2 (top-right) shows the effect of distributing the loads, and further more the effect of location of the $F_T$ distribution. As the extent of distribution range increases, the crack depth becomes slightly shallower. As shown in the graph, as the $F_T$ distribution shifts more to the rear (right) of the tool contact the crack grows substantially shallower, which is consistent with experimental results.

Benefits
- The results support the choice of more negative rake angles for lower surface crack or spall depth.
- The results also provide a sense of how sensitive surface crack and spall depth are to force level and distribution (rake angle).
- The results also provide a sense of how sensitive surface crack and spall depth are to material properties of fracture toughness and elastic modulus.
Parallel Process Machining — Its Mathematical Definition and Stability

**Introduction**

Parallel-process machining (PPM), from an intuitive perspective, occurs on machine tools that have either multiple spindles or multiple slides. The first known applications appeared early in the twentieth century due to their potential to increase productivity. Those applications include simultaneous boring of multiple engine cylinders and using multi-drill heads to generate hole patterns. Modern applications also include simultaneous turning of journals on engine camshafts and multi-spindle screw-cutting operations. Specific advantages of PPM may include:

- reduced cycle time, including rapid traverse and workpiece handling (idle) times,
- shorter tool change times and machine changeover times,
- improved accuracy due to the elimination of multiple set-ups,
- floor-space savings up to 30%.

**Approach**

Industrial interest in PPM has been growing due to the aforementioned potential advantages. A benchmark survey done by the authors concluded that most major machine-tool manufacturers produce parallel-process machine tools, with PPM being particularly common in transfer-lines. While some research has focused on operations/planning issues, little research has addressed the mechanical issues, such as maximizing productivity by understanding and modeling the process-to-process interactions. This effort aims to take a first step in that regard by mathematically posing the problem and then formulating an analytical stability solution for the case of symmetric (i.e., having identical conditions) processes. For the discussion to follow, a cutting process involves the removal of a chip at a single cutting tooth whereas a machining process involves one or more teeth experiencing the same kinematics (cutting and feed motions).

**Results**

The intuitive perspective of PPM relates to the number of tools being used on the machine — for instance, two tools machining one or two workpieces (2T1W or 2T2W, respectively). However, from a mathematical perspective, PPM relates to the number of dynamically dependent cutting processes. Dynamic dependence means that the relative tool-work displacements of the cutting processes in question are not related through kinematics alone but rather through a dynamic (frequency-dependent) coupling. This mathematical perspective is best understood by illustrating with a multi-tooth machining process, such as face milling.

From the intuitive perspective, a multi-tooth milling process is single-process in nature. First, consider the workpiece to be (comparatively) rigid. If the relative tool-work displacements at each tooth are related kinematically, a case that arises when the face mill cutter-body is “rigid”, the face-milling process is single-process in nature, mathematically speaking. In contrast, if the relative tool-work displacements at each tooth are not related through kinematics alone, such as when the cutter-body itself is flexible, the face-milling process is parallel in nature, again, mathematically speaking. An analogous case (1T2W) occurs for a single rigid face mill simultaneously cutting either multiple workpieces or a single workpiece with spatially varying dynamics (e.g., the deck of an engine block).

The above demonstrates how an intuitively single-process case is actually parallel in nature. That said, the opposite can hold — that is, intuitively parallel multiple processes need not be parallel in nature from the mathematical perspective. Consider two relatively rigid turning tools attached to opposite sides of a slide. If the slide itself rigidly connects these tools while a dominant flexibility exists in the machine bed to which the slide is attached, and furthermore there is no appreciable workpiece coupling, then these two machining processes act as a single, multi-tooth, machining process. In other words, the two cutting processes are dynamically independent, and thus this is not a case of PPM.

In the single-process problem, the dynamic tool-work displacement at each tooth is characterized by the response at any single location. For N-dimensional dynamics (where the displacement is an N-dimensional vector), the off-diagonal terms of the frequency response matrix represent dimension-to-dimension coupling and ultimately have no effect on stability. In PPM (single- or multi-dimensional), the response vector required to characterize the machining process includes responses at more than one location in the system. As such, the off-diagonal terms represent location-to-location coupling in addition to dimension-to-dimension coupling, the former being of critical importance in the PPM stability problem.

The analysis formulated applies only for cases with process symmetry (i.e., same speed, feed and depth of cut). For a case of structural symmetry as well, Fig. 1 shows how the overall solution is a superposition of the solutions corresponding to in-phase and out-of-phase chatter responses. The analytical solution is compared to experimental results (non-symmetric structural dynamics) in Fig. 2 showing good agreement.

**Benefits**

- Perplexing effects of flexible cutters and spatially varying workpiece dynamics are explained through the mathematical definition of PPM.
- Limits on process-to-process dynamic coupling may be specified so that a PPM application does in fact result in productivity and cost benefits relative to using multiple single-process machine tools.
Stability of Ultrahigh-Speed Machining

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Sponsor: None

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Introduction
It is well known that when increasing spindle speed to increase productivity the stability limit — the limiting depth of cut — undergoes repeated increases and decreases. These increases and decreases result in peaks in the stability limit at well understood speeds — those where the dominant modal frequency is an integer multiple of the tooth frequency. The two highest-speed peaks are usually wide enough to permit operation up in the peak. Machining in this speed range is often referred to as high-speed machining. Beyond the final highest-speed peak, the limiting depth of cut is understood to increase continuously with a further increase in speed. Operating at spindle speeds above about 1.5 times the dominant modal frequency is referred to here as ultrahigh-speed machining.

Of initial interest were the basic effects of intermittency on stability. Intermittency, or more generally periodic time variation, is present in milling processes as well as continuous boring and turning processes where the dominant structural mode is associated with the workpiece. Early in the study an added-lobe was found to exist at ultrahigh speeds for cases where periodic time variation exists. The objective is to understand this non-traditional mode of instability that limits the continual increase in limiting depth of cut that is presumed to occur as spindle speed (tooth frequency) increases into the ultrahigh-speed regime.

Approach
The added set of stability lobes found to exist for periodic process loading had previously gone unnoticed. Here, a one-dimensional orthogonal process is employed to facilitate basic understanding rather than practical utility. Simulation and experimental study are used to provide guidance for the development of an analytical solution. The degree of intermittency is quantified here by duty cycle. The tooth (spindle) duty cycle, \(O_t = O_s\), where \(N\) is the number of teeth and \(O_s\) can exceed unity.

The experiment apparatus is shown in Fig. 1. Tests are conducted under fundamental straight-edged orthogonal cutting conditions to allow the focus to be on the basic effects of intermittency, not tooth form or process kinematics. The setup allows vibration in the vertical direction while providing support that minimizes rocking induced by intermittent loading.

Results
A numerical simulation study provides insight to the fundamental effects of intermittency on stability. One of the primary parameters of interest is the duty cycle, the effects of which are shown in Fig. 2, where the normalized tooth frequency is the tooth frequency divided by the natural frequency. As the duty cycle approaches unity the added lobe collapses toward the continuous machining solution (see Fig. 2 (bottom)). The effects of structural stiffness, mass, natural frequency and specific energy on the added lobe are consistent with those observed in traditional stability analyses, except that the chatter frequency is always one-half the tooth frequency. Structural damping, while strongly affecting traditional stability, has relatively little effect on the added lobe. The added lobe exists for both single- and multi-tooth machining; it is dictated by the spindle duty cycle not the tooth duty cycle. Perhaps the most important fundamental finding is that the added lobe exists even when there is no regeneration of past vibrations. The extent of regeneration is modeled by the well-known overlap factor \(\mu\). The simulations show that the added-lobe boundaries can be found by scaling the case of a unity overlap factor by \(2/(1 + \mu)\).

Physical experiments conclusively show the added lobe to exist across multiple duty cycles. As shown in Fig. 3 (top), stability results of the physical experiment compare well with the numerical simulation results. Chatter frequency results confirm the chatter frequency to be one-half the tooth frequency whenever machining in the added lobe.

Analytical solution results can be carried to any “order” \(O\) to yield multiple added lobes \((O\) added lobes\). The un-damped solution brings to light a numbering convention where the un-damped added lobes reach down to the horizontal axis (zero depth/width of cut) at speeds where the normalized tooth frequency is \(2(2O - 1)\); the respective lobes are called the \((2O - 1)\)/2 lobe. Therefore, the ultrahigh-speed added lobe is the “1/2” lobe, the next is the 3/2 lobe, and so on. In addition to the general advantage of analytical solutions — physical insight not achievable given the black-box nature of simulation — the analytical solution can be used to quickly explore effects of parameters. Shown in Fig. 3 (bottom) is the effect of structural damping noted earlier.

Benefits
- Structural damping becomes unimportant if the process can be operated at ultrahigh speeds, keeping in mind that higher-frequency/stiffness dynamic modes of the structure could eventually enter the picture.
- The unbounded increase in limiting depth of cut as spindle speed increases comes only at even higher speeds than originally thought.
An Experimental Study of Fixed-Interface Dynamics under Harmonic Multi-Dimensional Loading

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Introduction
Research on the dynamic characteristics of fixed joints has to date focused on the response to time-varying loads in either the normal or tangential direction. However, joints in real structures are commonly subjected to loading that occurs simultaneously in both directions. There has been virtually no study of this problem of simultaneously time-varying multi-dimensional loading, although numerous investigators have noted the need for such, as recently as the 1990s.

A related work of the group addresses the modeling of interface response to generically time-varying loads in both directions and use of the model to study the special and common case of harmonically time-varying loads in both directions. The objective here is to experimentally investigate the effects of simultaneously harmonic normal and tangential loading on the dynamic response of a simple, uniformly loaded, annular interface. The results are intended to improve the basic understanding of the mechanics and provide validation of the model developed in the related effort.

Approach
The experimental apparatus is designed to meet the following requirements:

- simultaneous generation of time-varying loads normal to and tangential to the plane of the interface,
- control of the relative magnitudes of and phase difference between the normal and tangential loads,
- application, control, and measurement of the normal preload on the joint interface, and
- real-time measurement of the normal and tangential loads and displacements at the interface.

The resulting system is shown in Fig. 1 in which most of the primary mechanical subsystems are highlighted. They include (see Fig. 1 (top)) the preloading system, the force actuators, the interface, and (see Fig. 1 (bottom)) the subsystems for displacement measurement, load measurement and load transmission. The transmission links are designed to minimize its stiffness in the direction perpendicular to its loading direction to avoid introducing additional stiffness to the interface.

The experimental procedure takes care to assure that other spurious effects do not contaminate the joint dynamics of interest. Prior to testing the interface surfaces are cleaned with acetone to assure dry contact. Before each set of tests at a given preload the surfaces are ‘reset’ by reducing the preload to zero and then setting the preload to the desired value. The displacement sensors are then calibrated based on five repetitions using the differential micrometer stages upon which they are mounted.

To eliminate the slight cycle-to-cycle variation and tangential walk seen in the “steady-state” force-versus-displacement data, multiple cycles of data are averaged to obtain a single-cycle averaged steady-state response.

For model comparison purposes, parameters are identified as follows:

- The asperity-level coefficient of friction is approximated using that of the interface, which is identified by gradually increasing the tangential load under a constant normal load until gross slip occurs.
- The elastic properties are taken as their bulk values from tables.
- The surface parameters are identified as a single group that accounts for asperity density and the mean asperity radius. They are identified by matching the model predictions to test data for the case of a tangential load of zero, a preload of 800 N and a normal load amplitude of 400 N.

Results
The applied normal and tangential loads are, respectively,

\[ N(t) = P + N \sin \omega t \quad \text{and} \quad T(t) = T \sin (\omega t + \phi), \]

where \( N \) and \( T \) are the harmonic amplitudes and \( P \) is the normal preload.

A selection of experimental tangential force versus displacement loops is shown in Fig. 2 (top) for various phases and relative to a baseline for which only a tangential load exists (i.e., \( N = 0 \)). The trends in curvature and shift in area concentration from one end of the loop to the other across phase are consistent with model results. The main difference is that the model results show significant narrowing/widening for the 90°/270° phases, which are not predicted by the model. Another representation of the loops is the equivalent stiffness and energy dissipation (enclosed area). The latter results are displayed in Fig. 2 (bottom), as a function of phase, across a range of loads. The results are consistent with the model for phases of 180° to 360°.

Regarding tangential walk, even the case of constant normal load amplitude shows significant tangential walk, unlike zero walk seen in the model predictions for the same. This is consistent with observations in other experimental studies of pure tangential loading. The degree of walk for each phase is consistent with the model results, although the direction is not symmetric about zero as it is in the model results.

Benefits
- It is conceivable that joints can be oriented to achieve a loading amplitude ratio and/or phase that optimize damping and/or stiffness.
- The apparatus conceived and implemented here allows for future experimental studying of specific joint types (e.g., bolted, spot welded) and interfaces (e.g., adhesive) to support design and modeling efforts.

Fig. 1 Experimental apparatus (left) and close-up of force transmission links and displacement stage probes. Fig. 2 Experimental results.
Introduction
A fixed joint can be defined as any interface between two solids intended to experience no gross relative motion. Whether it is a bolted joint in a machine tool, a riveted joint in an aircraft structure, or a clamp or locator acting on a workpiece held in a fixture, the basic interfacial dynamics are the same. The focus here is on the basic modeling of a uniformly loaded interface, not a specific joint and its geometrically non-uniform loading (e.g., a bolted joint).

Past works have addressed the modeling of a uniformly preloaded interface subjected to an applied time-varying load in either the normal or tangential direction. The model presented here extends those works to account for generically time-varying bi-directional loading, where generically refers to the time variation being not necessarily periodic or harmonic, and bi-directional implies that both loads (normal and tangential) are time varying. While the model and its numerical implementation are valid for these conditions, it is demonstrated by exploring the effects of phase and relative load amplitudes under harmonically time-varying bi-directional loading — a type that occurs in many physical structures. One such example is the original motivator of this undertaking — the dynamics of machine-tool joints subjected to machining process loads.

Approach
Interfacial dynamics are dictated by the interactions that occur at the asperity level. A dynamic surface interaction model embodies two basic building blocks — an asperity-interaction model, which describes the mechanical response of mating asperities, and a statistical surface model, which describes the distribution of asperity characteristics across the surface. Research on asperity interaction modeling has addressed the two together. One that did so considered constant asperity level. A dynamic approach acting on a workpiece held in a fixture, the basic interfacial dynamics of machine-tool joints subjected to machining process loads.

The reformulated asperity interaction model is then combined with the statistical-surface model of the Greenwood and Williamson (GW) approach. The two rough surfaces in contact are approximated as an ideal rigid flat in contact with an equivalent rough surface, as shown in Fig. 3. The resulting model retains both the analytical value and measurability of the inputs that are inherent to the GW and Hertz-Mindlin approaches.

Results
Figure 4 (top) shows the hysteretic loops for simulations made for a preload of $P = 800$ N, a tangential load amplitude of $T = 110$ N and a normal load amplitude of $N = 110$ N (i.e., $N/T = 1$). Also shown is a baseline for which only a tangential load exists (i.e., $N = 0$). For consistency, each graph is for the fifteenth loading cycle, at which point some of the loops are not closed because the response is not at steady state. The baseline hysteretic loop for $N = 0$ is consistent with previously published closed-form solutions, taking on its steady-state path after the first one-quarter cycle (initial loading). When $N ≠ 0$, the loops become noticeably skewed (more pointed at one end) and walk substantially. Both the transient walks and skewness are the same in magnitude but opposite in direction for phases of $0^\circ$ and $180^\circ$ and for phases of $90^\circ$ and $270^\circ$.

As can be seen from Fig. 4 (bottom), the energy dissipation per cycle is maximized at phases around $90^\circ$ and $270^\circ$. Analogous to the varying effects of phase on the walking and skewness, the peaks are not quite symmetric about $90^\circ$ and $270^\circ$. Though not shown here, the equivalent static stiffness decreases and then increases between $0^\circ$ and $360^\circ$, reaching a minimum at $270^\circ$.

Both the energy dissipation and equivalent stiffness for $N ≠ 0$ are at times greater than, and at other times less than, their respective values during purely tangential loading. This behavior is dictated mainly by the value of the preload and its ratio to the normal load amplitude.

Benefits
- Model-based orientation of joints to optimize damping and/or stiffness on the load amplitude ratio and/or phasing seen by the joint.
- Model-based preload specification to balance stiffness and damping.