Characterization of the cutting forces generated during the gear hobbing process: Spur gear

N. Sabkhia, C. Pelaingre, C. Barlier, A. Moufki, M. Nouari

Abstract

The main goal of the current study is to propose a model for determining the cutting forces taking into account the complex kinematics of the hobbing process. This model is based on one hand on the CAD geometrical simulations of undeformed chip generated by hobbing process and on the other hand on a mechanistic model. In this mechanistic approach, the specific force coefficients have been obtained from a 2D numerical model. As a result of this investigation, the evolution of cutting forces was used to analyze the effect of the cutting parameters on the machinability of the machined material.

Keywords: Gear hobbing, Kinematics of cutting, Modelling, Specific cutting forces, Generated chip, Machinability

1. Introduction

The high performance of the gear transmission system is obtained through the high quality of involute gears. Among the various methods of manufacturing gears, hobbing is the most widely applied manufacturing process for the construction of involute gear in industry, [1, 2]. The prediction of cutting forces involved during the machining process is of great importance.

The hobbing process is a complex metal removal technique compared to conventional machining operations, such as turning and milling, [2]. The kinematics principle of the process is based on three relative motions between the workgear and the hob tool. These motions have to be synchronized to produce the required gear. The workgear rotates around its axis of symmetry with a constant angular velocity, synchronized with the relative rotation of the hob around its own axis and the axial feed of the hob (see Fig. 1).

The majority of research conducted these last years in the area of gear hobbing is focused on the physical mechanisms occurring at the cutting zone and during the chip formation process, [2, 3]. Some authors have been investigated the hob geometry, cutting forces measurement, the chip formation process during machining and the tool wear prediction [4–6]. Moreover, several authors developed various simulations codes of the gear hobbing process, such as SPARTApro and HOB3D software, [7, 8]. Numerical models have been developed using Computer-Aided Design (CAD) [1, 2, 9], Finite Element Analysis (FEA) [3, 10] or analytical based approaches, [11, 12]. Using these models, the prediction of the machining parameters such as chip geometry, chip flow, tool wear, cuttingforces, etc… become possible considering the real workpiece, tool and production data, [1, 5, 6, 13–15].

The work presented above emphasizes more the hobbing process for the roughing of gears which are characterized by their small size (a few millimeters). Few studies have been interested in finishing hobbing gears, [7, 16]. In fact, the research studies on the hobbing process for finishing with very large sizes (diameter > 1 meter) are not numerous. This type of operation is studied in the present paper. It is characterized by specific parameters compared to small gears. For example, the machining time of one big gear is about several hours while it is about only a few seconds for small gears. One hob tool can be used to machine several small gears while it is...
used to machine just one big gear. The weight and dimensions of this kind of parts are also specific parameters to this type of operation.

In this paper a new methodology was developed to predict the hobbing forces components from the analysis of orthogonal cutting tests and the calculation of undeformed chip thickness. For this purpose, a new mechanistic approach of the cutting process has been used. Thus, the force coefficients (cutting and edge effects) have been obtained from a numerical model based on the ALE approach. This model enables to analyze precisely the thermomechanical effect of the cutting edge on the chip formation during the hobbing operation of big part.

2. Modelling of the hobbing process

2.1. Kinematics of hobbing process

As mentioned before, hobbing is a highly geometrical complex process compared to conventional machining. Manufacturing simulations facilitate the analysis of machining processes, [17]. In the literature, many authors studied virtual kinematics simulations for the hobbing process, [1, 2]. The aims being to introduce a mathematical model to calculate the shape and thickness of the various undeformed chip created for every hob tooth.

In gear hobbing, the hob tool and the workgear move in a linked revolution ratio. These revolutions are synchronized with the hob axial-feed (see Fig. 1-(b)). Such kinematics are used to generate a specific geometry of involute gears.

In order to simulate the kinematics of hobbing process, a new CAD model has been developed in the current work with CATIA software. It has been used to calculate the undeformed chip profiles from the geometric intersection between the tool and the work-piece. To implement this model, the geometry of the hob tool and the workgear was defined from several parameters as shown in Fig. 1. These parameters are the module (m), the number of teeth (z₂), the outside diameter (d₉), the helix angle (aₕ), the gear width (w) and the pressure angle (αₙ). In order to complete the kinematics chain of the hobbing process, other parameters of the hob tool must be defined. These parameters are the outside diameter of hob (d₉), the number of columns (nᵢ), the number of hob origins (z₁), the helix angle of the hob (γₕ) and the axial pitch (ε). Other parameters can also be defined such as the depth of cut (t), the axial feed (fₐ), the cutting speed (Vc) and the setting angle of the hob relative to the workgear (η). This angle is of great importance in the hobbing process. It is calculated from the difference between the helix angle of the workgear (hₙ) and the helix angle of the hob (γₕ).

The CAD model of the tool was generated thanks to the 3D digitization process using the Breukmann system. The Breukmann apparatus allows scanning of the real hob tool geometry in the form of points cloud set. These recorded space points are then rebuilt to obtain a representative CAD model. The average measuring error was estimated at 0.2mm. Regarding the 3D model of the work-piece, it is built from the main geometrical parameters of the gear to the semi-finished state. To be validated, the model was compared with the 2D profile of the part supplied by the manufacturer.

From these definitions, a CAD program was created to calculate the shape and thickness of the undeformed chip. This program is based on four coordinated systems. The two coordinate systems (1) and (2) are positioned on the work-piece. They have a Z-axis running through the workgear’s axis. In the coordinate system (2), the Y axis is always rotating toward the direction of the gap center while the axes of coordinate system (1) are fixed. The two coordinate systems (3) and (4) are positioned on the hob tool. The coordinate system (4) is associated to the examined tooth and
has the Z axis parallel to the hob’s axis, the X axis perpendicular to Y and Z axis. The Z axis of the coordinate system (3) coincided with the hob’s axis and it was used to calculate the total cutting forces exerted on the tool, Figs. 1 and 3.

The simulation of kinematics of the hobbing process is established by the transfer of all cutting motions on the hob tool in order to optimize the calculation time. This means that the part was held stationary and the tool moved. Concerning the initial position of the hob relative to the work-piece, the hob’s axis is inclined by the setting angle ($\eta$) relative to the horizontal plane (X1Y1) and is parallel to the plane (X1Z1). Then, the setting up of the hob is made via the centering of the reference tooth in a gap gear. It is the tooth which has the local axis X4 parallel to local axis Y2 when it passes through the center of the gap. Finally, the positioning of the cutting edge is carried out by creating a 3D point on the hob. This point is placed in the center of the hob tool. The movement of the hob center forms a helix around the workgear. The helix radius is equal to $d_h^2 + d_t^2 - t^2$, the helix pitch is equal to $f_a$ and its axis is the Z axis of the coordinate system (2).

In order to identify the columns of the hob tool, they are numbered. The column which contains the reference tooth is denoted by “column 0”. The column that passes after column “0” is column “1” and the column previous to that is column “13” respectively. The hob tool contains 14 columns in total.

Based on this model, the 3D chip profile was calculated from two surfaces. The first is defined by the assembly of different generating tool positions. The second surface is defined from the surface of the gap gear before machining. The intersection of these surfaces forms the 3D volume of the undeformed chip (see Fig. 2). In order to calculate accurately the 3D chip generated during the cutting finishing operation, the generating tool positions are determined specifically. In fact, the intersection calculated on the first round on the hob and for each column allows the profile of finishing gear to be obtained with an attack in full cut of the tool, regardless of the machined material in the previous turn. The calculation is therefore not representative of reality. The hob tool is assumed to machine through the top of the workgear to finish at the bottom. So to determine accurately the uncut chip section, during cutting, the previous pass has been considered.

2.2. Modelling and prediction of the cutting forces components

In order to determine the cutting forces, the mechanistic approach developed by Armargero and Epp. [18] has been adopted. The cutting force $F_v$ and the thrust force $F_f$ are calculated from the local undeformed chip thickness as given by Equation (1). The specific edge coefficients ($K_{ei}$) and the specific cutting coefficients ($K_{ci}$) were determined from a 2D numerical model of an elementary orthogonal cutting operation, see Subsection 2.3.

$$F_i = K_{ei}hv + K_{ci}bb; \quad i = v \text{ or } f$$

where $h$ is the uncut chip thickness and $b$ represents the width of cut.

In order to determine the cutting forces during the hobbing process, the mechanistic model was applied with a discretization method of the cutting edge. Fig. 3 illustrates the principle cutting edge discretization of the hob’s tooth. The first step in the calculation is the identification of five different zones. These five zones consist of three linear edges (zone 1, 3 and 5) and two rounded parts (zone 2 and 3). To take into account the real cutting edge geometry, the engaged part in the cutting of the rounded nose is broken down into a set of cutting edge elements. Thus, each elementary chip produced by a straight cutting edge element, is obtained from an orthogonal cutting operation. The section area of the undeformed chip $bh$, machined by a cutting edge element, is determined by using CAD software and the corresponding elemental cutting forces $dF_v$ and $dF_f$ are calculated from Eq. (1). Finally, the total forces components exerted on the hob tool are obtained by integrating elementary force components as given by Eqs. (2) and (3).


\[
\dot{\mathbf{R}}_{\text{tool}} = \begin{cases} 
F_i = \sum_j (dF_i \sin \theta_j - dF_j \sin \psi_j \cos \theta_j) & \text{if } j = \text{zone 1} \\
F_j = \sum_i (-dF_i \cos \theta_i - dF_j \sin \psi_j \sin \theta_j) & \text{if } j = \text{zone 2} \\
F_i = \sum_j (-dF_i \cos \theta_i - dF_j (\theta) \cos \psi_j) & \text{if } j = \text{zone 3} \\
F_j = \sum_i (-dF_i \cos \theta_i - dF_j (\theta) \cos \psi_j) & \text{if } j = \text{zone 4}
\end{cases}
\]

(2)

With:

\[
\begin{align*}
\alpha_s, \psi = -1, & \quad j = \text{zone 1} \\
\frac{\pi}{2} - \alpha_s, \psi = -1, & \quad j = \text{zone 2} \\
\frac{\pi}{2}, \psi = -\alpha_s, \ & \quad j = \text{zone 3} \\
\frac{\pi}{2} - \alpha_s, \psi = +1, & \quad j = \text{zone 4} \\
\alpha_s, \psi = +1, & \quad j = \text{zone 5}
\end{align*}
\]

(3)

In these equations, \( \theta \) is the angular position of the cutting tooth, \( \alpha_s \) the pressure angle of the hob, \( \alpha_l \) the inclination angle of the elementary edge in zone 2, and \( \alpha_k \) the inclination angle of the elementary edge in zone 4.

2.3. Numerical calculations

Using the ALE approach with the Abaqus/Explicit FE code, 2D numerical simulations of orthogonal cutting have been performed, [19]. The ALE (Arbitrary Lagrangian-Eulerian) approach is considered as the most appropriate method to simulate the thermo-mechanical process of continuous chip formation. A uniform mesh has been adopted for the work-piece (about 20 \( \mu \)m of size) and for the cutting tool (188\( \mu \)m). The simulated machining time is about 20ms. With this approach, the initial geometry of the chip has to be defined in terms of the initial chip thickness and the tool-chip contact length.

The modelling of the chip formation process is generally performed by considering the tool-workpiece couple. Only the vicinity of the cutting zone is considered in the numerical model. For the tool-workpiece couple, the solution corresponding to the thermo-mechanical problem should satisfy simultaneously and at any time the following mechanical and thermal balance equations (4-5):

\[
\nabla \cdot \sigma + f_i = \rho \ddot{u}
\]

(4)

\[
\nabla \cdot \nabla T + \rho c_p \dot{T} + q_i = 0
\]

(5)

\( \sigma \) is the Cauchy stress tensor, \( f_i \) the body force density, \( \dot{u} \) the acceleration, \( T \) the temperature, \( \rho \) the material density, \( \lambda \) the thermal conductivity, \( c_p \) the specific heat capacity, and \( q_i \) the volumetric heat generation.

The balance equations are strongly coupled. The stress tensor \( \sigma \) depends on the temperature \( T \) via the material behaviour laws, Equation (6). Also in the thermal balance Equation (5), the volumetric heat generation \( q_i \) in the work material is mainly due to the plastic work, depending on the strain, strain rate and temperature. The different non-linearities (geometrical, material behaviour and contact) make the analytical resolution of the two equilibrium equations (4) and (5) impossible in practice, even in the 2D case, as considered in the present work. Numerical approaches, like the Finite Element Method (FEM), are generally necessary to solve this system of equations.

The mechanical and thermal properties of the work-piece (AISI 4337) and the cutting tool (ASP 30) are given in Table 1.

Table 1. Mechanical and thermal properties of the used work-piece and cutting tool.

| Material | Work piece | Tool |
|----------|------------|------|
| E-modulus (GPa) | 210 | 240 |
| Hardness (HRC) | 33 | 65 |
| Poisson’s ratio | 0.3 | 0.28 |
| Density (Kg/m³) | 7.84 | 8.1 |
| Thermal conductivity (W/m.K) | 37.7 | 24 |
| Specific heat (J/Kg.K) | 460 | 420 |
| \( T_m \) (K) | 1700 | - |
| \( T_0 \) (K) | 298 | 298 |

The thermo-mechanical behaviour of the work-piece is assumed to be isotropic and thermoviscoplastic with the following constitutive:

\[
\sigma = \left[ A + B \left( \tau^p \right)^n \right] \left[ 1 + C \ln \left( \frac{\tau^p}{\tau_0^p} \right) \right] \left[ 1 - \left( \frac{T - T_0}{T_m - T_0} \right)^\lambda \right] \]

(6)

where \( A, B, C, m \) and \( n \) are the material parameters, \( \tau^p \) the Von Mises equivalent plastic strain, \( \tau^p \) the Von Mises equivalent plastic strain rate, \( \tau_0^p \) the reference equivalent plastic strain rate, \( T_m \) and \( T_0 \) are, respectively, the material melting temperature and the reference ambient temperature. The Johnson–Cook law parameters of the workmaterial AISI 4337 are reported in Table 2.

Table 2. Mechanical and thermal properties of the used work-piece and cutting tool.

| \( A \) (MPa) | \( B \) (MPa) | \( n \) | \( C \) | \( m \) |
|-------------|-------------|------|------|------|
| 850 | 356 | 0.304 | 0.072 | 0.513 |

The friction along the tool-workpiece interface has a significant effect during machining, since it directly affects the thermo-mechanical contact loading. As part of the FE modelling, the local friction at the tool-chip interface (i.e. at any contact point) is modelled by the modified Coulomb friction law, where the friction stress is limited by the current shear flow stress of the work material, written as follows:

\[
\tau_f = \min (\tau^+(\mu, \sigma^e))
\]

(7)
where $\tau_s$ is the shear friction stress, $\sigma_n$ is the normal contact stress, $\mu_{loc}$ is the local friction coefficient and $F = \sigma f \sqrt{3}$ is the current shear flow stress of the work material at the contact interface. The case where $\tau_s = \mu_{loc} \sigma_n$ is known as the sticking contact, and the case where $\tau_s = \tau_r$ is known as the sliding contact. The sticking contact occurs close to the tool tip and the sliding contact occurs where the chip leaves the rake face and at the end of contact at the flank face. In addition, the heat exchange at the tool-workpiece interface has two origins, the frictional heat ($q_f$) and the heat conduction across the interface ($q_c$) due to the thermal contact resistance, which is written as follows:

\begin{equation}
\begin{aligned}
\dot{q}_w &= \beta \dot{q}_f + \dot{q}_s, \\
\dot{q}_c &= (1-\beta) \dot{q}_f - \dot{q}_s,
\end{aligned}
\end{equation}

With:

\begin{equation}
\begin{aligned}
\dot{q}_f &= \eta_f \tau_s v, \\
\dot{q}_s &= h (T_w - T_t)
\end{aligned}
\end{equation}

where $\dot{q}_w$ and $\dot{q}_c$ are heat flux densities going into the tool and work-piece, respectively, $v$ is the sliding velocity, $\tau_s$ the friction stress given by Eq. (7) and $\eta_f$ the frictional work conversion factor. $\beta$ is the heat generation coefficient, which defines the fraction of friction heat generated at the tool-chip interface ($\beta \dot{q}_f$) and the complementary part (i.e. $(1-\beta) \dot{q}_f$) on the work material side. The latter may depend on other interface parameters (e.g. the sliding velocity $v$). $T_w$ and $T_t$ are temperatures on two elemental surfaces in the work-piece and tool under contact, respectively. The corresponding tool-work material interface parameters are reported in Table 3.

Table 3. Tool-workpiece interface parameters.

| $\beta$ | $\eta_f$ | $h$ | [W/m$^2$/K] |
|---------|----------|-----|--------------|
| 0.5     | 1        | 2000|              |

3. Experimental study and model validation

Several experiments have been conducted to validate the cutting forces predicted by the numerical model. Orthogonal cutting tests were performed on tubes made of the low steel alloy AISI 4337 with an outside diameter of 42mm and thickness of 2mm. The tool used was taken from straight edges on the teeth of the monobloc hob tool (see Fig. 4). The aim of this is to use the same material composition as the real industrial hob. The material tool is the high-speed-steel ASP30 (HSS).

The selected cutting parameters are reported in Table 4. For each test, the cutting forces were recorded using a Kistler dynamometer (Type 9257B) attached to the tool holder and connected to data acquisition software through a multi-channel charge amplifier.

Table 4. Machining parameters.

| Parameters               | Value |
|--------------------------|-------|
| Cutting velocity [m/min] | 9.30  |
| Feed rate (f) [mm/rev]   | 0.05  |
| Width of cut [mm]        | 2     |
| Lubrification            | Neat cutting oil |
Table 5. Average cutting force coefficients as calculated from the numerical model of the orthogonal cutting tests.

| Orthogonal cutting  |  |  |  |
|---------------------|-----------------|-----------------|-----------------|
| Kcv (N/mm²) | 3127.08 | Kef (N/mm) | 1180.18 |
| Kef (N/mm) | 10.44 | Kf (N/mm) | 416 |

Fig. 7 presents an example of calculation of cutting force components using the procedure described above. This graph illustrates the variation of the cutting forces corresponding to the reference hob’s tooth.

Fig. 7. Cutting forces calculation for the reference hob’s tooth during hobbing process.

4. Conclusion

The mechanistic approach proposed in this work was applied to predict cutting force components generated during the hobbing process. This new methodology was developed for predicting the real and complex cutting forces in hobbing. It consists of the simulation of the undeformed chip with CATIA software and then the calculation of the instantaneous chip cross. The cutting forces were determined from a mechanistic model for which the specific cutting and edge coefficients are obtained from a 2D Finite Element model. A good agreement was found between orthogonal cutting tests and the numerical calculations. Thus, the present approach can be used to analyze precisely the evolution of cutting forces during the complex hobbing process.

References

[1] Tapoglou N et Antoniadis A. CAD-Based Calculation of Cutting Force Components in Gear Hobbing , J. Manuf. Sci. Eng., 2012, vol. 134, n°3, p. 8.
[2] Dimitriou V et Antoniadis A. CAD-based simulation of the hobbing process for the manufacturing of spur and helical gears, Int. J. Adv. Manuf. Technol., 2009, vol. 41, n°3-4, p. 347-357.
[3] Bouzakis KD, Friderikos O, et Tsiafis I. FEM-supported simulation of chip formation and flow in gear hobbing of spur and helical gears, CIRP J. Manuf. Sci. Technol., 2008, vol. 1, n°1, p. 18-26.
[4] Bouzakis KD, Kombogiannis S, Antoniadis A, et Vidakis N. Gear Hobbing Cutting Process Simulation and Tool Wear Prediction Models , J. Manuf. Sci. Eng., 2002, vol. 124, n°1, p. 42.
[5] Stein S, Lechthaler M, Krasnitzer S, Albrecht K, Schindler A, et Arndt M. Gear Hobbing: a Contribution to Analogy Testing and its Wear Mechanisms, Procedia CIRP, 2012, vol. 1, p. 220-225.
[6] Bouzakis KD, Lili E, Michailidis N, et friderikos O. Manufacturing of cylindrical gears by generating cutting processes – A critical synthesis of analysis methods, Manuf. Technol., 2008, vol. 57, p. 676-696.
[7] Klocke F, Gorgels C, Stuckenber a, et Schalaster R. Software-Based Process Design in Gear Finish Hobbing, Gear Technol., 2010 vol. 27, n°3, p. 48-52.
[8] Tapoglou N et Antoniadis A. Hob3D: a novel gear hobbing simulation software, in World congress on engineering, 2011, p. 6-8.
[9] Dimitriou V, Vidakis N, et Antoniadis A, Advanced Computer Aided Design Simulation of Gear Hobbing by Means of Three-Dimensional Kinematics Modelling, J. Manuf. Sci. Eng., 2007, vol. 129, n°5, p. 911–918.
[10] Antoniadis A, Vidakis N, et Bilalis N. Fatigue Fracture Investigation of Cemented Carbide Tools in Gear Hobbing, Part 1: FEM Modelling of Fly Hobbing and Computational Interpretation of Experimental Results, J. Manuf. Sci. Eng., 2002, vol. 124, n°4, p. 784.
[11] Bouzakis KD. Konzept und technologische Grundlagen zur automatisierten Erstellung optimaler Bearbeitungsdaten beim Wälzfräsen, Rhein-Westfälische Tech Hochsch Aachen Germany, 1980.
[12] Antoniadis A, Vidakis N, et Bilalis N. Fatigue Fracture Investigation of Cemented Carbide Tools in Gear Hobbing, Part 2: The Effect of Cutting Parameters on the Level of Tool Stresses–A Quantitative Parametric Analysis, J. Manuf. Sci. Eng., 2002, vol. 124, n°4, p. 784-791.
[13] Klocke F, Gorgels C, et Stuckenberry A. Investigations on Surface Defects in Gear Hobbing, Procedia Eng., 2011, vol. 19, p. 196-202.
[14] Gerth J, Werner M, Larsson M, et Wiklund U. Reproducing wear mechanisms in gear hobbing-Evaluation of a single insert milling test, Wear, 2009, vol. 267, n°12, p. 2257-2268.
[15] Klocke F, Gorgels C, Weber GT, et Schalaster R. Prognosis of the local tool wear in gear finish hobbing, Prod. Eng., 2011, vol. 5, n°6, p. 651–657.
[16] Tenji T. Finish Hobbing of Hardened Gears with carbide Hobs, Eng. Dept Kashifju Works Ltd, 2010, p. 1-14.
[17] Klocke F, Gorgels C, Schalaster R, et Stuckenberry A. An Innovative Way of Designing Gear Hobbing Processes, Procl. Int. Conf. Gears, 2010, p. 393-404.
[18] Armargo EJA et Epp CJ. An investigation of zero helix peripheral up-milling, Int. J. Mach. Tool Des. Res., 1970, vol. 10, n°2, p. 273-291.
[19] ABAQUS Documentation for version 6.11-2 Dessault systems Simulia, 2011.