COMPUTER SIMULATION OF INDUCTION SURFACE HARDENING AXIALLY SYMMETRIC WORKPIECES

Darko Landek, Franjo Cajner, Tomislav Filetin

Faculty of Mechanical Engineering and Naval Architecture University of Zagreb
Ivana Lucica 5., HR10000 Zagreb

Abstract

To achieve a higher quality process planning and a more flexible execution of induction hardening computer simulation of electromagnetic and thermodynamic processes as well as microstructural transformation is recommended. This paper describes and suggests an own simulation model of the induction hardening process and explains a special simulation program that has been developed for the surface induction hardening of axially symmetric steel workpieces. The simulation program performance has been tested by experiments in some cases of high frequency induction hardening cylindrical specimens made of DIN 42CrMo4 steel grade. The measured values of surface hardness and hardening depths have been compared with the simulated values and a good correspondence between simulated and measured values has been obtained

Key words: surface induction hardening, process simulation, mathematical modelling

1. INTRODUCTION

Surface induction hardening is a process of local heating and quenching a thin surface layer (0,25 to 2,5 mm) of a steel or cast iron workpiece with the purpose to improve wear resistance, contact strength and to increase residual compressive stresses, while allowing the remainder of the workpiece to be unaffected by the process. There are number of excellent textbooks that describe in detail the basic physical processes during surface induction hardening [1-3]. The process of induction hardening must be planned and performed in such a manner that specified requirements are satisfied such as hardness of the surface layer, the layer depth and shape. All required properties of the hardened layer will be achieved only if the specific details of the process have been taken into consideration during the design stage of the workpiece, and correct execution of process has been provided. Process planning includes also design and fabrication of the induction coil of the best-suited shape and dimensions and selection of exact process parameters [4]. This calls for the mathematical modelling and computer simulation of the physical processes during surface induction hardening such as electromagnetic and thermal processes, microstructural transformations, stresses and distortion development. Mathematical models for mentioned processes are different, ranging from simple experience-based expressions for only one physical phenomenon to complex systems of equations for connected physical phenomena that can be solved only through application of numerical methods and computer simulation [3,5].

This paper describes and suggests an own basic simulation model of the induction hardening process for axially symmetric parts. These parts make the major group of induction hardened items (e.g. shafts and axles, bearing, pins, bolts, pistons, cylinders). Mathematical simulation includes models of the most important physical phenomena in occurring the workpiece, and their interactions during induction hardening such as transfer of electromagnetic energy from the inductor towards the workpiece, surface layer heating, spray cooling and the microstructural transformations in steel. These phenomena determine surface hardness, hardness depth, hardness distribution along the workpiece radius and shape of the hardened zone which are sufficient for successful set-up of induction hardening process of axially symmetric parts. Presented simulation model is applied using own developed software.

2. MATHEMATICAL MODELLING AND SIMULATION OF INDUCTION HARDENING

The developed mathematical model is appropriate for simulation of the high frequency (HF) induction hardening ($f > 50$ kHz) of axially symmetrical parts applying rotation and rotation with

scanning method. Model includes simulation of electromagnetic, heat and microstructural phenomena in the surface layer of the workpiece. Electromagnetic phenomena are simulated applying boundary elements method (BEM), while heat phenomena are simulated applying finite elements method (FEM). Because of interlacing, simulation of electromagnetic and heat phenomena are made simultaneously. Then, microstructure and hardness of each finite element was predicted from their calculated thermal cycles.

2.1 Simulation of electromagnetic processes

Mathematical modelling and simulation of transfer of energy from inductor to the workpiece and induction of eddy currents within the workpiece in the case of HF electromagnetic field has been done by applying approximate boundary elements method [6]. Approximate BEM is used to calculate eddy currents on the surface of the workpiece, and there is no need to calculate values of electromagnetic field in the workpiece surrounding. Considering numerical modelling for rotation-feeding hardening of axially symmetric parts allows setting up a two-dimensional model in a cylindrical coordinate system which is also valid for a three-dimensional axially symmetric body.

Mathematical model for calculation of density of eddy currents on the surface of the workpiece is derived from the phasor form of Maxwell equations for harmonically changing and quasi-stationary electromagnetic field, assuming that there are eddy currents in the surface layer of the workpiece generated by the current flow in the inductor coil and that magnetic permeability of the material is non-linearly changing, depending on the induced magnetic field [3, 7]:

$$\nabla^2 \underline{\underline{A}} = \mu(j\omega\kappa \underline{\underline{A}} - \underline{\underline{J}}_2) - \mu \left[\nabla \times \left[\underline{\underline{A}} \times \nabla \left(\frac{1}{\mu} \right) \right] \right] \quad (1)$$

In this phasors (or complex vectors) equation $\underline{\underline{A}}(j\omega, r, \theta, z)$ is phasor of the magnetic vector potential, $\underline{\underline{J}}_2(j\omega, r, \theta, z)$ (A/mm²) is phasor of current density in the induction coil, μ (H/m) is the magnetic permeability, ω (rad/s) is the circular frequency, κ (S/m) is the electric conductivity, $j = \sqrt{-1}$, and $\nabla \times$ stands for differential operator for rotation of the vector field. In cylindrical coordinate system for induction heating of axially symmetrical parts (Fig.1) equation (1) could be counted only for angular component of the magnetic vector potential ($\underline{\underline{A}}(j\omega, \theta) = A_\theta$). When applying BEM, equation (1) is formulated two times: in the first step for the workpiece ($\underline{\underline{J}}_2 = 0$) and in the second step for the space around the workpiece ($\kappa = 0$, $\mu = \mu_0$). Relationship between these equations is established through boundary conditions of continuity and equilibrium of magnetic vector potential on the surface of the workpiece Γ_1 :

$$\underline{\underline{A}}_{1\theta} = \underline{\underline{A}}_{2\theta}, \quad \frac{1}{\mu} \left(\frac{\partial \underline{\underline{A}}_{1\theta}}{\partial n_1} + \frac{\underline{\underline{A}}_{1\theta}}{r} n_r \right) = - \frac{1}{\mu_0} \left(\frac{\partial \underline{\underline{A}}_{2\theta}}{\partial n_1} + \frac{\underline{\underline{A}}_{2\theta}}{r} n_r \right) \quad (2)$$

and condition $\underline{\underline{A}}_{2\theta} \rightarrow 0$ when $|r| = \sqrt{r^2 + z^2} \rightarrow \infty$. $\underline{\underline{A}}_{1\theta}$ is the phasor of magnetic vector potential on the workpiece side, $\underline{\underline{A}}_{2\theta}$ is the phasor of magnetic vector potential on the side of induction coil, n_1 is a normal to the workpiece surface, μ is the magnetic permeability of the workpiece surface, and $\mu_0 = 4\pi 10^{-7}$ H/m. System of equations derived from (1) and boundary conditions (2) is solved by dividing surface of the workpiece into constant boundary elements and setting up integral equations for boundary element [6,7]. Because of the symmetric workpiece shape, it is sufficient to consider just a half of the longitudinal cross-section, (Fig.1). For the sake of completeness of the equation system, distribution of the induced eddy current density is assumed applying an approximate equation (3) according [6]:

$$\underline{\underline{J}}_1(\xi) = \underline{\underline{J}}_{01} e^{-\sqrt{j\omega\kappa\mu} \cdot \xi} = \underline{\underline{J}}_{01} e^{-(1+j)\xi \sqrt{\frac{\omega\kappa\mu}{2}}} = \underline{\underline{J}}_{01} e^{-(1+j)\frac{\xi}{\delta}}, \quad \underline{\underline{J}}_{01} = -j\omega\kappa \underline{\underline{A}}_{\theta 1} \quad (3)$$

The assumption of exponential distribution of eddy current density in the surface layer of an axially symmetric workpiece is valid, provided relationship between workpiece radius (r) and skin depth (δ) is

higher than 10. This condition has been met in the performed experimental work. Calculation of induced magnetic vector potential on the workpiece surface (A_θ) applying approximate BEM is an iterative procedure, in which correction of the value of magnetic permeability of a boundary element, applying calculated value of magnetic field intensity and temperature in the center of boundary element, is done for each time increment.

Magnetic permeability was modelled depending on the temperature and magnetic field intensity. The curve of magnetic permeability dependent of magnetic field intensities at the room temperature (293 K) was obtained from [8] and stored as discrete data points. Assuming the theoretical behavior of the relative magnetic permeability at the magnetic saturation state as a function of temperature it can be approximated by modified Weiss theory [9,10] and own simple model:

$$\frac{B_{s(T)}}{B_{s(293K)}} = \tanh\left(\frac{B_{s(T)}}{B_{s(293K)}} \frac{T_{Currie}}{T}\right), \quad \mu_{rs(T)} = \frac{B_{s(T)}/B_{s(293K)}}{C(1-T/T_{Currie})^{\frac{3}{2}}}, \quad \mu_{r(T)} = \left(1 - \frac{B_{s(293K)} - B_{s(T)}}{B_{s(293K)}}\right) \mu_{rs(293K)} \quad T < T_{Currie} \quad (4)$$

Here, T (K), T_{Currie} (K) are the absolute temperature and the Currie point temperature, $B_{s(293K)}$ (T), $B_{s(T)}$ (T), are the saturation magnetisms at 293 K and at the elevated temperature, C is the constant, $\mu_{rs(T)}$ (1) is a saturation relative magnetic permeability and $\mu_{r(T)}$ (1) is a relative permeability, both dependent of temperature. The approximation of relative magnetic permeability calculated by equations (4) is displayed in Fig.2. It is reasonably good, because it is impossible to calculate exactly the value of relative magnetic permeability dependent of magnetic field intensity and temperature at elevated temperatures. The values of electric resistance dependent on temperature were calculated with the spline interpolation of experimental results given in [11]. The values of relative magnetic permeability and electric resistance were calculated and iterative corrected in every time increment for a particular boundary element depending of temperature in the element.

Figure 1: Induced high frequency eddy current in the electric conductive workpiece

Figure 2: Curves of relative magnetic permeability calculated as functions of magnetic field intensity and temperature

Convergence of the BEM solutions for a time increment is achieved after 5 to 10 iterations. From equation (3) and convergent BEM solution, the magnetic field intensity induced in the surface (H_{0i} , A/m) and specific power of Joule heat sources (P_{JQi} , W/m³) are calculated for each boundary element (i).

$$H_{0i} = \left| (1 + j) \sqrt{\frac{\omega \kappa_i}{2 \mu_i}} A_{\theta} \right|, \quad P_{JQi}(\xi) = \frac{1}{2} \omega \mu_i H_{0i}^2 e^{-2\xi \sqrt{\frac{\omega \mu_i \kappa_i}{2}}} \quad (5)$$

The specific power P_{JQ} is included in the mathematical model of thermal processes as the main heat source within the edge zone of the workpiece, while magnetic field intensity is used for correction of values of magnetic permeability for boundary element for the next time increment.

2.2 Simulation of thermal processes

Computer simulation of thermal processes applicable for induction hardening applying rotation and scanning movement includes simulation of heating of surface layer and subsequent quenching. Energy conservation law is set up for the entire workpiece. Non-stationary thermodynamic model is solved by the finite elements structured mesh. Application of adaptive finite elements mesh is foreseen for the further research. In simulation of the scanning inductive hardening the workpiece moves through the induction coil and spraying ring (Fig.3). This could require repeatedly generation of the mesh for each workpiece displacement. However, if mathematical model is set up accounting for relative movement of the induction coil and spray ring to the steady workpiece, then physical description of induction hardening is not changing. If the boundaries of thermodynamic system are placed on the workpiece only, then the simulation of heating and cooling may be performed on the steady workpiece applying the same mesh for all time increments with repeated calculation of values for thermodynamic boundary conditions for each time increment [12]. For an axially symmetric volume dV of the workpiece the Fourier's heat-diffusion equation is derived from the energy conservation law [2, 3]:

$$c\rho \frac{\partial \vartheta}{\partial t} - \frac{1}{r} \frac{\partial}{\partial r} \left(\lambda r \frac{\partial \vartheta}{\partial r} \right) - \frac{\partial}{\partial z} \left(\lambda \frac{\partial \vartheta}{\partial z} \right) = P_{jQ} \quad (6)$$

where ϑ (°C) is temperature, ρ (kg/m³) is specific density, λ (W/mK) is thermal conductivity and c (J/kgK) is specific thermal capacity. To solve Fourier equation (7) initial temperature of the workpiece equal to the ambient temperature is assumed, $t = 0$ s, $\vartheta_0 = \vartheta_{\text{amb}}$. At the workpiece surface there are two zones, the heating zone where the intensive generation of heat appears caused by induced eddy currents and the quenching zone where the heat dissipates through coolant. Thermodynamic boundary conditions for certain particular zones of the workpiece surface are shown in Fig. 3. In the heating zone there is heat dissipation as a result of convection (q_{konv} , W/m²) and radiation (q_{rad} , W/m²) processes, and in the cooled (quenching) zone heat is removed by spraying action of the water jet having heat flow density (q_Q , W/m²). Densities of mentioned heat flux are calculated applying locally distributed coefficients of heat transfer (α , W/Km²) [2, 3, 13]:

$$q_{\text{konv}} = \alpha_0 (\vartheta_s - \vartheta_0), \quad q_Q = \alpha_Q (\vartheta_s - \vartheta_0) \quad (7)$$

$$q_{\text{rad}} = C_s (T_s^4 - T_0^4) = C_s (T_s^3 + T_s^2 T_0 + T_s T_0^2 + T_0^3) (T_s - T_0) = \alpha_{\text{rad}} (\vartheta_s - \vartheta_0) \quad (8)$$

where ϑ_s (°C), i.e. $T_s = \vartheta_s + 273$ (K) is the surface temperature, ϑ_0 is the ambient temperature, and C_s (1/100⁴K⁴) is coefficient of radiation dissipation. The other, not yet heated areas of surface, above the induction coil, have temperatures which are the same as the ambient one ($\vartheta_s = \vartheta_0$). A Neumann boundary condition $\partial \vartheta / \partial r = 0$ is defined along the axis of symmetry providing solution of equation (6) just for one half of the longitudinal cross-section. Temperature values for the other half are obtained by symmetric mirroring. The listed boundary conditions (7) and (8) are linking heat flux dissipated through the workpiece surface and temperature gradient at the surface:

$$-\lambda_s \left(\frac{\partial \vartheta_s}{\partial r} n_r + \frac{\partial \vartheta_s}{\partial z} n_z \right) = q_s \quad (9)$$

λ_s (W/mK) is thermal conductivity of the workpiece, and q_s (W/m²) is the heat flux dissipated through workpiece surface into the ambient space or quenching coolant. Local values of heat flux q_s , i.e. values of α_0 and α_{rad} coefficients are calculated iteratively for each time interval of the hardening process, taking into account known relationships among temperature dependent thermal properties and the value of surface temperature (ϑ_s) in the given location on the surface.

Particular considerations are necessary in calculation of heat transfer coefficient (α_Q , W/m²K) when spraying with coolants undergoing Leidenfrost phenomena (water, oils, aqueous polymer solutions) is applied. Depending on the surface temperature, different modes of heat transfer may occur such as film, or nucleate boiling and convection (Fig. 4). Therefore the developed simulation program allows calculation of the heat transfer coefficient only for spray quenching. As reported in [13], value of heat transfer coefficient (α_Q) when applying particular quenching coolant is mostly affected by following

factors: surface temperature (ϑ_s), material of the workpiece and impingement density (m_0 , kg/m²min). Impingement density (m_0) is determined as mass of quenching coolant sprayed onto the heated workpiece surface unit area per time unit.

Figure 3: Volume of the workpiece as a thermodynamic system

Figure 4: Linear approximation of changes of heat transfer coefficient when pure water spraying is applied, depending on workpiece surface temperature.

Fourier's differential equation for heat conduction (6) including boundary conditions (7) to (9) is a non-linear and non-stationary problem in a two dimensional r - z space which is solved in the developed simulation program applying finite elements method with weak Galerkin formulation [14]. To provide simplicity and faster calculation, yet assuring reasonable accuracy, three nodes, axially symmetric triangular elements are selected to turn half of the workpiece volume into discrete elements. The size of triangular elements near the surface in the heating zone is order of magnitude of a minimum skin depth. Other triangular elements over cross section of a workpiece are bigger.

Thermal properties of material are considered to be constant for a triangular finite element, but they are changing from one to another element depending of temperature in the element center. The values of specific density, thermal conductivity and specific thermal capacity dependent of temperature were obtained from [11]. These thermal properties were calculated by spline interpolation from stored data for an average element temperature and iterative corrected in every time increment. After 3 to 5 iterations the convergence of FEM solutions is achieved. Then, the next BEM and FEM computation are done for the next time increments. The Joule power P_{JQ} exists only in the heating zone near the surface to the maximum depth of five 'skin depths'. Its distribution over skin depth is approximated by equation (5) where ξ is normal distance of triangle node from the workpiece surface. The value of Joule power P_{JQ} in a certain triangle element in the heating zone is calculated with linear Gauss numerical integration. In time space, equation (6) is solved applying Galerkin time-stepping schema that providing unconditional stability of equation solution [14].

2.3 Simulation of microstructural phase transformations

Induction hardening includes a heating step (austenitization) and a cooling step (quenching). Kinetics of microstructural phase transformations during austenitization is monitored in the developed model through digitalized recording of TTS (time-temperature-solubility) diagram [15] and continuous-cooling-transformation (CCT) diagram (Fig.5). For particular finite element calculation of mean heating rate (v_a ,

K/s) is done when raising temperature to the austenitization range ($\vartheta_a, ^\circ\text{C}$) during heating time from 700°C to ϑ_a ($\Delta t_{700-\vartheta_a}, \text{s}$):

$$v_a = \frac{\vartheta_a - 700}{\Delta t_{700-\vartheta_a}} \quad (10)$$

After that estimation of hardness and microstructural phases appearing after queching is done applying digitalized record of CCT diagram and calculated cooling time ($t_{8/5}, \text{s}$) for 800 to 500°C range [15].

Figure 5: Schematic presentation of the temperature cycle and microstructural transformations in the characteristic zones at induction hardening of surface layer of a carbon sub-eutectoid steel grade

3. RESULTS

The whole surface of a cylinder (Fig. 6) made of 42CrMo4 steel grade should be hardened to a depth of $1+0.8 \text{ mm}$. The cylinders were hardened applying scanning with rotation method. Workpieces were heated using a high frequency power supply (410 kHz, power rating 50 kW). Since the austenitizing temperature depends significantly on the heating rate, these two conditions for as - delivered (normalized) structure of 42CrMo4 steel grade have been selected from [15]. For the specified heating rate of 100-300 K/s, required austenitizing temperature is 850°C . This steel grade also must be cooled at lower cooling rate to avoid occurrence of surface cracking. Therefore, quenching was done applying 18% water solution of PAG at impingement density of $8,77 \text{ kg/m}^2\text{min}$ with spraying ring at 5 mm below the inductor.

Simulation has been performed applying several different scan speeds within range of 4 to 10 mm/s, applying operating power within range of 15 to 45 kW. The objective was to establish a combination of parameters that will produce required depth of hardening and surface hardness. Simulation yielded two sets of possible operating parameters: power rating 30 kW at scan speed 4 mm/s, and power rating 45 kW at scan speed 10 mm/s. The computing time for performing the simulations was appreciable long – about 10 to 16 hours for 50 mm of induction scan hardening of the above mentioned specimens. The simulations were carried out using the 533 MHz processor with 128 MB RAM memory and the Matlab programming language with its executable shell. The own software based on Matlab code is still under

development and testing. With further code transfer to the C++ language computing time will be shorter and the accuracy of the solution will be better, which are important for the workshops practice.

Figure 6: Test specimen setting in induction coil and quench ring

Figure 7. Distribution of hardness in the transversal cross-section of specimen (Fig. 7) made of 42CrMo4 steel grade applying different parameters ($f = 410$ kHz)

With each set of listed parameters (a and b), trial hardening was performed, each series containing 3 specimens. Specified power rating of the source was verified by measurements of current and voltage on the anode of the vacuum tube and recording the time changes of current and voltage in the inductor. The listed parameters (power, scan speed, quench intensity) were kept at the constant level during the induction hardening process.

Rockwell C method for testing of surface hardness has been used for all induction hardened cylinders. Measured values were 57 ± 2 HRC (equivalent to 640 – 675 HV according to DIN 50190). Distribution of hardness across the depth of hardened layer was tested on the metallographically prepared cross-sections of cylinders applying Vickers method with 9,81 N load (HV1), (Fig. 7).

From Fig. 7 it may be noted that both sets of parameters result in similar distribution of hardness. In both cases, hardness of approximately 690 HV1 was achieved at the surface. Micrographic investigation has revealed martensitic microstructure in the edge layers. Though the applied sets of parameters produced different hardening depths, both values meet the required specification.

4. CONCLUSION

Adequate preparation of induction hardening process is a necessary condition for realization of specified properties of the hardened workpiece. Because of complex phenomena within the workpiece during the induction hardening process, setup of necessary operating parameters may be a major problem. Currently, this problem may be solved more efficiently and faster than in past applying specialized simulation programs.

By development of proprietary simulation program intended for less complex axially symmetric parts one condition for better preparation of induction hardening process has been provided. The simulation program has been tested for the case of a simple cylindrical workpiece, and achieved results were within limits of acceptable deviations. Further investigation should be directed to improvement of the simulation program for more complex axially symmetric parts, and to simulation of residual stresses and distortions.

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