



Article **Process Signature for Laser Hardening**

Friedhelm Frerichs ^{1,2,*}, Yang Lu ^{2,3}, Thomas Lübben ^{1,2} and Tim Radel ^{2,3}

- ¹ Department of Materials Engineering, IWT, Bremen Badgasteiner Str. 3, 28359 Bremen, Germany; Luebben@iwt-bremen.de
- ² Transregional Collaborative Research Centre 136 "Process Signatures", University of Bremen, Badgasteiner Str. 3, 28359 Bremen, Germany; lu@bias.de (Y.L.); radel@bias.de (T.R.)
- Department of Laser Processing, BIAS, Badgasteiner Str. 3, 28359 Bremen, Germany
- Correspondence: frerichs@iwt-bremen.de

Abstract: During many manufacturing processes for surface treatment of steel components heat will be exchanged between the environment and the workpiece. The heat exchange commonly leads to temperature gradients within the surface near area of the workpiece, which involve mechanical strains inside the material. If the corresponding stresses exceed locally the yield strength of the material residual stresses can remain after the process. If the temperature increase is high enough additionally phase transformation to austenite occurs and may lead further on due to a fast cooling to the very hard phase martensite. This investigation focuses on the correlation between concrete thermal loads such as temperature and temperature gradients and resulting modifications such as changes of the residual stress, the microstructure, and the hardness respectively. Within this consideration the thermal loads are the causes of the modifications and will be called internal material loads. The correlations between the generated internal material loads and the material modifications will be called Process Signature. The idea is that Process Signatures provide the possibility to engineer the workpiece surface layer and its functional properties in a knowledge-based way. This contribution presents some Process Signature components for a thermally dominated process with phase transformation: laser hardening. The target quantities of the modifications are the change of the residual stress state at the surface and the position of the 1st zero-crossing of the residual stress curve. Based on Finite Element simulations the internal thermal loadings during laser hardening are considered. The investigations identify for the considered target quantities the maximal temperature, the maximal temperature gradient, and the heating time as important parameters of the thermal loads.

Keywords: laser hardening; process signature; thermal loads; residual stress; initial microstructure; mechanical behavior

1. Introduction

Manufacturing processes have an impact on the functional performance of components [1–3]. On the one hand these impacts are not always wanted. Especially finishing processes such as grinding or hard turning affect not only the requested features but also generate sometimes thermal effects which change the workpiece surface layer properties, e.g., residual stresses, microstructure, and hardness. For example, high residual tensile stresses or large stress gradients may lead to reduced lifecycle performance because of easier crack initiation and propagation. If a surface hardening is wanted a selective heat treatment on the other hand can lead to an optimal surface hardness and residual stress state. These examples make clear that the most precise knowledge of plastic deformations and modified microstructures, which are generated by the considered processes, is necessary.

However, up to now it is very difficult to generate predefined surface layer properties after machining processes [2]. Especially if basic production methods change, the process



Citation: Frerichs, F.; Lu, Y.; Lübben, T.; Radel, T. Process Signature for Laser Hardening. *Metals* **2021**, *11*, 465. https://doi.org/10.3390/ met11030465

Academic Editor: Wei Zhou

Received: 10 February 2021 Accepted: 8 March 2021 Published: 11 March 2021

Publisher's Note: MDPI stays neutral with regard to jurisdictional claims in published maps and institutional affiliations.



Copyright: © 2021 by the authors. Licensee MDPI, Basel, Switzerland. This article is an open access article distributed under the terms and conditions of the Creative Commons Attribution (CC BY) license (https:// creativecommons.org/licenses/by/ 4.0/). quantities have to be redefined and the resulting properties must be redetermined [4]. This procedure should be accelerated and improved. To accomplish that, a material-orientated view is applied that considers physical and chemical quantities which are responsible for the modifications. These quantities will be called in this paper *internal material load*. The Transregional Collaborative Personal Control (CPC) "Process Signatures" funded by the

Transregional Collaborative Research Centre (CRC) "Process Signatures" funded by the German Research Foundation, resumes the so-called concept of Process Signatures [5]. The term *Process Signature* is already applied for hot stamping processes [6]. It describes the shape of the strain envelope and depicts the strain state of contiguous elements in a stamping process. In terms of the CRC "Process Signatures" it means the correlation of internal material loads and material modifications. The advantage of Process Signatures is that this approach considers not necessarily a concrete process.

To develop Process Signatures appropriate descriptive quantities of the internal material loads must be elaborated which correlate with the considered modifications. The descriptive values include the process parameters further on only in an implicit way. In prior papers Process Signatures with mainly thermal impacts were developed for grinding and induction heating processes [7]. In this regard the temperature increase due to an induction coil and a grinding wheel which transmit heat into the material are considered (Figure 1). Both processes generate heat by moving heat sources which have their own characteristic features and physical conditions. Each process type changes the temperature distribution inside of the workpiece in its own characteristic manner and generates therefore characteristic distributions of plastic strains and phase distributions.



Figure 1. Basic models of external thermal loads due to grinding (a), induction (b) and laser heating (c).

The current work extends the existing Process Signatures [7] to laser hardening and will give therefore a contribution to confirm the general validity of the prior assumptions which are elaborated for grinding and induction hardening. In particular, the investigated load spectrum consisting of "surface heat source with mechanical load" and "volume heat source without mechanical load" is extended by the combination of "surface heat source without mechanical load" (Figure 1).

Laser surface hardening is a process to increase the hardness of the surface layer and introduce the compressive residual stress, which further improve the wear resistance as well as fatigue strength of a hardenable steel. After decades of investigations, laser hardening has been well used in the industry, for example in spur gears [8] and splined shafts [9].

The modification of hardness and residual stress is a result of austenitizing and martensite generation in a temperature cycle. Laser hardening is normally applied on steels with carbon weight percentage between 0.4 and 1.5 [10]. In contrast to the conventional transformation hardening using flame or in a furnace, a fast heating and cooling cycle can be realized by a focused laser beam as moving heat source and subsequent self-quenching. As the specimen is irradiated by the laser beam, a fast heating process allows the setting of temperatures above the start temperature of austenitizing A_{c1} , followed by the dissolution of cementite into carbon and diffusion of carbon into newly formed austenite grains. The longer the temperature keeps above A_{c1} , the more of the original phase is transformed

into austenite [11]. Due to the fast heat conduction in the base material, a so-called selfquenching process enables the transformation from austenite to martensite.

Laser irradiation conditions are considered to be the important factors to influence the hardening results [11]. To study the laser irradiation conditions, modeling serves as an efficient approach to understand the effect of beam shape or scanning strategies on the temperature field and the change of microstructures. The optimization of laser hardening was conducted by adapting the intensity distribution to generate a top-hat temperature distribution using freeform optics [12]. Rather than a top-hat intensity distribution, the best possible shape for a surface heat source would be a rectangle with two radial quadrants on each side. In terms of scanning strategies, there are, at present, two approaches: The first method uses a large beam spot in size of dozens of millimeters and moving in one direction. Another method applies a small beam spot in size of some millimeters and scanning bidirectionally (laser spot scanning speed) perpendicular to the moving direction (laser head moving speed), with typical overlapping tracks. Based on the latter approach, the influence of laser spot scanning speed on the hardening results was studied experimentally and numerically [13]. It is found that scanning speed over 750 mm/s generate a relatively smooth hardened track, which is similar to the conventional laser hardened track using the first approach. In addition, models by extracting the punctual and geometrical attributes from the measured hardness profile are built to predict the shape of the hardness profile depending on the laser parameter for controlling laser hardening quality [14]. Furthermore, for the effect of surface topography on the laser energy coupling, Volpp et al. [15] investigated the laser hardening of flat and curved surface and the corresponding hardening qualities. Nevertheless, no significant impact of the surface shape on the microstructures could be identified.

The main involved mechanisms which generate material modifications are for all three considered processes expected and mainly equal. However, from a quantitatively point of view the processes generate significantly different internal material loads. The similarities and the differences of the processes should be elaborated to support the finding of suitable process parameters. The goal of the investigations concerns the question of influences of the considered internal material loads.

2. Objective and Procedure

To compare the residual stress distributions generated by the heating processes three target quantities are considered for grinding and induction heating. These are: the residual stress on the surface and the positions of the zero-crossings of the residual stress curve (Figure 2a). In case of surface hardening generally compressive residual stresses on the surface occur. Hence the 1st zero-crossing of the residual stress curve is from compressive to tensile stress, the 2nd zero-crossing from tensile to compressive stress. Figure 2b gives two examples. It illustrates the influence of the initial microstructure on the development of surface residual stress for two different quenched and tempered states with an initial hardness of 30 and 47 HRC. The main difference between both states is the phase transformation kinetic and the yield strength [16].

The descriptive value for the target quantity residual stress on the surface (Figure 2a) is the maximal temperature gradient $(grad \theta)_{max}$ normal to the surface which occurs during the process (Figure 2b). A possible mechanism orientated explanation for that behavior is that the gradient is mainly responsible for thermal strains and stresses and therefore responsible for the development of stresses.

The next target quantity is the depth of the 1st zero-crossing of the residual stress profile from pressure to tensile stress. The depth of the 1st zero-crossing and the depth of 50% martensite amount occur at nearly the same position [7]. The square of the maximal temperature increase multiplied by the square root of the heating time $\Delta \theta_{max}^2 \cdot \sqrt{t_c}$ was proposed as a descriptive value for the depth of the 1st zero-crossing. The heating time t_c is according to Figure 1 the quotient of l_g and v_{ft} . It is the time in which a point at the surface of



the workpiece is in direct contact to the power of the moving heat source. In contrast to the first descriptive value, the mechanism which can explain the parameter $\Delta \theta_{max}^2$ is unknown.

Figure 2. (a) Typical residual stress profiles after interaction of thermally dominated processes and the target quantities of the considered material modifications. (b) The correlation of the maximal thermal gradient and the residual stress at the surface in case of grind hardening of two initial quenched and tempered states of AISI4140.

The product of the maximal temperature increase with the square root of the heating time $\Delta \theta_{max} \sqrt{(tc)}$ correlates with the zero-crossing from tensile to compressive stresses. In case of hardening this is commonly the 2nd zero-crossing of the residual stress [7]. The depth is hard to measure because it is mostly deeper than 2 mm. Therefore for residual stress measurements thick layers have to be electrolytic removed. For that reason they are not considered in this work.

The extension of the processes to laser hardening offers on the one hand in comparison to grind hardening a surface heating method, which generates no additional impacts on the workpiece. There are no additional forces and no chip removals. Therefore a pure thermal impact can be investigated. On the other hand, laser hardening works with significantly different process parameters, because the heat source is commonly much smaller than in case of grinding and induction heating processes. Hence it has to carry out with significantly higher power densities. Due to the laser beam size and the applied moving velocities much smaller Peclet numbers [17] than in case of grind hardening and inductive heating can be investigated. The dimensionless Peclet number is given by:

$$Pe = \frac{l_g \cdot v_{ft}}{4 \cdot \kappa} \tag{1}$$

 l_g is width of the moving heat source in moving direction and v_{ft} the velocity of the heat source. κ is the thermal diffusivity of the material. Although the material parameters are temperature dependent and the power profile in case of laser heating is not constant along l_g the Peclet gives a good estimation of the principal local and time dependent temperature evolution [17].

In case of grinding Peclet numbers higher than 1.8 are reached, whereby for laser hardening Peclet numbers lower than 0.8 are applied. Due to a comparison of laser hardened and grind hardened samples the effect of the applied Peclet numbers can be investigated. The investigations are carried out for quenched and tempered states of an AISI 4140 with annealing temperatures of 400 and 600 °C with a hardness of 47 HRC and 30 HRC respectively.

To elaborate Process Signatures, numerical methods are applied because the necessary data are very difficult and time consuming to measure. Additionally, numerical methods enable considerations of the inverse problem in which selected processes with their characteristic process parameters should produce specific results. The knowledge of the mutual dependency of thermal loads and process parameters ensure the possibility for solving the inverse problem.

To carry out the simulations the characteristics of the applied heat source of the process must be known. These are the local and time dependent heat which is absorbed by the workpiece and the local and time dependent loss of heat by the environment. Furthermore, the thermal and mechanical properties and the transformation behavior during austenitizing of the initial microstructure and martensite formation must be carefully measured, because the initial state influences the development of phase transformation and the mechanical behavior [16] (Figure 2b). Additionally, the properties of the phases austenite and martensite must be known.

Even after very precise determination of all these data a validation of the simulations is necessary. This validation includes temperature measurements on and below the surface to validate the complete local and time dependent temperature evolution of the considered workpiece. Without these measurements the reliability of the numerically achieved thermal loads would be uncertain. With similar arguments the necessity of measurements of the final modifications such as the final microstructure with its hardness distribution and the residual stress state after the heat treatment becomes clear.

3. Methods

3.1. Steel Grade AISI 4140 (42CrMo4)

In all cases the steel grade AISI 4140 was used. This material is a widely used steel for quenching and tempering and therefore often used for induction hardening processes. Many material data are available, e.g., for mechanical properties and phase kinetics. For that reason it was chosen as the standard steel for investigations within the CRC "Process Signatures". AISI 4140 is subject of research [17]. The steel grade has many industrial applications in engineering e.g., it is used for gears where the possibility of laser hardening was shown [18,19]. Table 1 gives the measured element concentration of the investigated steel melt.

Table 1. Element concentration of the used AISI 4140 melt.

Elements	С	Cr	Mo	Si	Mn
AISI 4140 (42CrMo4) [mass%]	0.43	1.09	0.25	0.26	0.74

3.2. Generation and Main Properties of the Initial Microstructures

The heat treatment for generating the quenched and tempered initial microstructure was carried out in a vacuum furnace. The time-temperature sequence is presented in Figure 3a. It leads to a quenched and tempered (QT) state with a hardness of 30 HRC (b). The microstructure is given in the middle picture of Figure 3, the characteristic mechanical properties on the right hand side (c). The measurements of the martensite start temperature M_S reveal 321 °C \pm 16 K. The final residual stress at the surface was less than 40 MPa. [20] The investigations of thermal and mechanical behavior were performed with the thermal-mechanical-testing machine Gleeble 3500. More details and further results are published in [16].

With a similar procedure quenched and tempered material with a hardness of 47 HRC was produced. The tempering temperature was instead of 600 °C only 400 °C. For more details compare [16].



Figure 3. (a) Heat treatment for generating the quenched and tempered initial microstructure with a hardness of 30 HRC (b), and the temperature and phase dependent yield strength σ_0 and phase independent Young's modulus (c).

3.3. Modeling of the Phase Transformation and Hardness Calculation

The austenitizing process is modeled with an approach proposed by Miokovic [21]:

$$f_A = 1 - exp(-(b(T) \cdot t)^{n_A})$$
⁽²⁾

with:

$$b(T) = C \cdot exp\left(-\frac{\Delta H}{kT}\right) \tag{3}$$

 f_A represents the amount of generated austenite, *T* is the temperature in Kelvin, ΔH is a measure for the enthalpy of transformation, *k* the Boltzmann constant. *C* and n_A are constants ($C = 2.84 \times 10^{20} \text{ s}^{-1}$ and $n_A = 1.525$) [16]. With this equation the starting temperature of austenite formation depends on the enthalpy of activation ΔH which varies with the initial microstructure. In case of the applied quenched and tempered state with 30 HRC the enthalpy resulted in 4.225 + / - 0.13 eV. For the 47 HRC variant this value is 4.055 eV [16]. ΔH governs together with the heating rate the A_{C1} and A_{C3} value, which is a measure for the beginning and ending of the phase transformation to austenite. The A_{C1}-value is reached by definition if f_A equals 0.01.

The phase transformation to austenite enables the formation of martensite if a subsequent quenching is carried out. The transformation austenite to martensite was modeled with a modified Koistinen-Marburger equation [22].

$$f_M = 1 - e^{-\left(\frac{Ms - \theta}{\theta_0}\right)^{n_M}}$$
(4)

where f_M represent the amount of generated martensite, M_S is the martensite start temperature. θ_0 and n_M are constants.

The experimental determination of martensite amounts is difficult, especially in case of simultaneously existing tempered microstructures. For the validation of the calculated phase proportions a model for hardness calculation proposed by Blondeau [23] was applied. The hardness measured in Vickers is given by:

HV (martensite) =
$$127 + 949 \text{ C} + 27 \text{ Si} + 11 \text{ Mn} + 8 \text{ Ni} + 16 \text{ Cr} + 21 \text{ Log}_{10} (\text{VR})$$
 (5)

VR is the temperature rate at 700 °C during the quenching period given in Kelvin/hour. For the initial microstructure QT_{30HRC} a hardness of 321 HV and for QT_{47HRC} 400 HV are assumed. In case of mixed phases a linear mixing rule is applied. The coefficients C, Si, Mn, Cr are the amounts of the corresponding alloying elements given in mass%. For the hardness of the retained austenite 100 HV is assumed. Bainite and ferrite/pearlite are not considered, because in the framework of the considered processes they are not generated.

Within a narrow strip temperatures between tempering temperature and A_{C1} are occur and generate an additional tempering. This effect is neglected within this work.

3.4. Experimental Setup for Laser Hardening

Laser hardening was conducted by using a multi-mode Yb:YAG (Ytterbium-doped: yttrium aluminum garnet) continuous-wave disk laser (Trumpf TruDisk 12002) with operating wavelength of 1030 nm. Two different fibers (core diameter of 200 μ m and 600 μ m) were used to guide the beam to the optic. The optic (Trumpf BEO D70) was equipped with a focusing and a collimation lens with 400 mm and 150 μ m focal length, respectively. Furthermore, the beam was coupled into a cylindrical lens, so that at focal position a rectangular beam with different beam width was generated, while the length perpendicular to the moving direction kept constant at 10 mm due to system design. The laser spatial intensity distribution and beam width was determined by a beam profiler (Ophir SP300). The process was conducted with protection gas of argon, so that oxidation of the treated surface was minimized. Additionally, the argon flow accelerates the cooling directly after passing the laser beam (Figure 4).



Figure 4. Setup for laser hardening of AISI 4140, with clamping.

The workpieces were 150 mm long, 12 mm wide and 15 mm high (Figure 5). The surface temperature was measured with the quotient pyrometer Sensor Therm Metis M3. To investigate the temperature evolution inside the workpiece sheath thermocouples type K with a diameter of \emptyset 0.25 mm were used. The tips of the thermocouples were placed in 6 mm deep vertical boreholes 1.0, 1.5, 2.0, and 2.5 mm from the heated surface. The 12 mm workpiece width was chosen to protect the thermocouples against the laser beam length L_{length} perpendicular to the feeding direction of 10 mm. The laser would destroy the thermocouples if the beam hits the thin wires of the thermocouples.



Figure 5. Setup for laser hardening of AISI 4140 with sheathed thermocouple typ K Ø 0.25 mm.

3.5. Residual Stress Measurements

To characterize residual stress distribution X-ray diffraction (XRD) was applied. The depth profiles were investigated up to approximately 1.5 mm depth using electrochemical etching of successive layers as described in Epp [24]. The measurements were performed with a Bragg-Brentano diffractometer (type MZ VI E, GE Inspection Technologies).

For calculation of the residual stresses, the software RayfleX from GE Sensing Inspection Technologies was applied. The peak position was evaluated using sliding gravity method with thresholds between 30 and 80% of the maximum intensity. The sin² ψ method was used to calculate the residual stresses using the $\frac{1}{2}$ S₂^{hkl} of the {211} diffraction peak of α -Fe. For all measurements, depth correction was applied to correct the stress values for the effect of the removed layers as described by Moore and Evans [25]. The mean error from the standard deviation of the sin² ψ measurements reached around ±20 MPa.

3.6. The Simulation Setup for Laser Hardening

According to the experimental investigations the simulation of laser hardening is performed with laser beams generated by the fiber core diameters of 600 and 200 μ m with Gaussian intensity profiles. The feed velocities v_{ft} vary between 0.5–20 mm/s, the mean heat fluxes \dot{q} vary from 10 to about 46 W/mm² (total power $P_{total} = 165-680$ W). The heat flux profile is assumed to be constant and hence temperature independent during each simulation procedure. As mentioned above the experimentally used optical components provide a constant laser beam length perpendicular to the moving direction of 10 mm. In comparison to sample width of 12 mm not the complete sample was applied with power. As mentioned above, this was necessary to protect the thermocouples. The same configuration was chosen for validation of the simulation.

However, for comparability reasons with the already calculated Process Signatures for grind hardening and induction hardening, the simulation study for laser hardening was carried out with a beam length covering the entire sample width of 12 mm.

3D simulations with the Finite Element code SYSWELD[®] were carried out for sample length of 30 mm, width of 6 mm, and height of 15 mm. The 30 mm length was chosen, because a stationary state was already achieved after 15 mm. The simulation model takes the symmetry of the problem into account (mirror symmetry with respect to the middle plane in axial direction). The time increment was chosen in that way that the heating period was calculated with about 1500 time steps. The fine mesh in Figure 6 consists of 35,000, the other of 17,500 cubic volume elements with eight Gauss points each.

A further question concerns the clamping conditions of the workpiece. An inhomogeneous temperature distribution leads to strains inside the material. The component reacts on the temperature strains with distortion which minimizes the stored elastic energy. In case of grinding, bending of the workpiece would change the infeed of the grinding process. Therefore grinding processes are commonly carried out with a fixation. Otherwise the amount of bending can be significant.

Although clamping is not necessary for laser hardening a numerical clamping will be applied for reasons of comparability with grinding investigations. Numerically the clamping is achieved via a spring constant, which prevents a bending of the heat-treated surface. The clamping is released at the end of the process. Hence all results belong at the end to a workpiece which sees no external forces.



Figure 6. (a) FE meshes for the simulations depending on beam fiber core diameter. (b) Simulated temperature distribution at the surface.

4. Results

4.1. Power Distribution of the Laser Beam

As shown in Figure 7 the spatial intensity distribution measurement shows a quasi-Gaussian intensity distribution along the short axis for the 200 and 600 µm fiber core diameter. To achieve the best model for simulation of the power distribution a Gaussian approach was chosen. The parameters of the approach were calculated by the least square method. The beam width l_g is defined by the intensity decrease down to 1/e of the maximal intensity. This value is given by $\sqrt{2} \cdot \sigma$, where σ is the standard deviation of the Gauss distribution. σ for the 600 µm fiber core is 523 µm. About 84% of the absorbed laser power coupled in within the strip $2 \cdot \sqrt{2} \cdot \sigma$. The 200 µm fiber core diameters lead together with the optic components to a standard deviation of 160 µm. The beam width l_g of the 600 µm fiber core is 1.48 mm, the 200 µm fiber core approximately 0.45 mm (Figure 7).



Figure 7. Measured and modeled intensity distribution of laser beams generated by fiber core diameter of 200 (a) and 600 µm (b).

4.2. Validation of Temperature Calculation

For comparison with rectangular shaped power profile an equivalent power density is defined. The equivalent power density allows the comparability of different shapes of power profiles [7]:

$$\dot{q}_{eq} = \frac{P_{total}}{l_g \cdot L_{length}} \tag{6}$$

As mentioned above, L_{length} is the length of the laser beam perpendicular to the moving direction. P_{total} is the total absorbed power of the heat source.

Figure 8a presents measured and simulated temperature data for a heating process with a fiber core diameter of 600 µm. The nominal laser power was 1000 W, the feeding velocity v_{ft} 2 mm/s. The simulation was performed with a total power P_{total} of 325 W (absorption 32.5%). This value would lead to an equivalent power density q_{eq} of 21.96 W/mm². The pyrometer respectively thermocouple measurements and the related simulations agree very well. In particular, the measured temperature front ahead and the cooling period after passing of the laser beam agree very well with the simulations. Figure 8b presents the calculated temperature evolution on the surface and compares this with the temperature gradients on the surface. It shows that the gradient first increases after the temperature has already risen to several hundred °C. The temperature gradient depends strongly on q/λ . The peak of the temperature gradient just before reaching the A_{C1} temperature occurs due to the minimal heat conductivity for that temperature.



Figure 8. (a) Temperature measurement for process with $l_g = 1.48$ mm (fiber core diameter 600 µm), $v_{ft} = 2$ mm/s, 1000 W laser power, absorbed power P_{total} 325 W (simulation), Gaussian power profile with standard deviation of 523 µm, (b) simulated temperature and temperature gradient at surface. The dashed lines indicate the current position of the laser beam.

4.3. Validation of Microstructure, Hardness, and Residual Stress Calculation

Figures 9 and 10 summarize the measured and simulated modifications of the same workpiece presented in Figure 8. In Figure 9, the calculated phase amounts (a) and the according micrographs are presented. In the middle (b) and right part (c) of the figure the hardened zone with a width of approximately $445 \pm 20 \ \mu m$ is clearly visible. The laser beam length of 10 mm has a considerable impact on the hardening result. It can be seen that the calculated depth of the hardened zone is lightly overestimated (Figure 9a,c).



Figure 9. (a) simulated phase amounts. (b,c) micrographs of the workpiece with QT_{30HRC} . Experiment with $l_g = 1.48$ mm, $v_{ff} = 2$ mm/s, 1000 W laser power, absorbed power 325 W (simulation), fiber core diameter 600 µm.



Figure 10. (a) Measured and simulated hardness and (b) residual stresses. QT_{30HRC} experiment with a Gaussian power profile ($l_g = 1.48 \text{ mm}$), $v_{ft} = 2 \text{ mm/s}$, 1000 W laser power, absorbed total power $P_{total} = 325 \text{ W}$ (simulation).

Figure 10 compares measured results with simulated data for the hardness and residual stress distribution. Similar to Figure 9, the calculated 1st zero-crossing seems slightly deeper than the measured value. The absolute values of the simulated compressive residual stresses in the surface near area are about 80 MPa higher than the measured data. Schwenk [26] founds similar deviations of measured and calculated data for quenched and tempered AISI40, too. Up until now, the reason for that deviation has not been clear. However, besides these deviations, the comparison of measured and simulated data reveals overall a good and sufficient quality for the target quantities.

4.4. Simulation Study for Laser Hardening

Sections 4.2 and 4.3 present experimental and related simulation results for a laser hardening process. To achieve Process Signature components for this process, further simulations are necessary. Table 2 presents the simulation parameters for the numerical determination of further points in the diagrams for Process Signature components. According to Section 2 the target quantities are the residual stress at the surface and the 1st zero-crossing of the residual stress distribution. The associated descriptive values are the temperature gradient (grad θ)_{max} normal to the surface for the residual stress at the surface. For the depth of the 1st zero-crossing $\Delta \theta_{max}^2 \cdot \sqrt{t_c}$ is chosen.

Laser Beam Width l_g [mm]	Feed Velocity v_{ft} [mm/s]	Heating Time t_c [s]	Total Power q _{total} [W]	Peclet Number Pe [-]	<i>θ_{max}</i> [°C]	(grad θ) _{max} [K/mm]	$\frac{\Delta\theta_{max}^2 \cdot \sqrt{t_c}}{[10^5 \text{ K}^2 \text{ s}^{1/2}]}$
1.48	0.5	2.960	165	0.017	1098	588	19.98
1.48	1	1.480	264	0.035	1120	729	14.73
1.48	2	0.749	323	0.069	1125	894	10.50
1.48	4	0.370	385	0.138	1129	1166	7.47
1.48	8	0.185	490	0.277	1123	1877	5.24
1.48	12	0.123	555	0.415	1077	2386	3.93
1.48	16	0.925	625	0.553	1072	2754	3.37
1 / 8	20	0.074	680	0.692	1052	3045	2 90

Table 2. Parameters applied for the simulation study and the resulting values for the considered descriptive values. The Peclet number is calculated with a mean thermal diffusivity κ of 10.7 mm²/s. The parameters are applied for the initial microstructures QT_{30HRC} and QT_{47HRC}.

Figure 11 presents the results of this study. The diagrams show the correlations between material modifications and the corresponding load descriptive and are therefore components of the Process Signature for laser hardening. For the simulated surface residual stress the differences between both quenched and tempered variants are not large. Additionally, some measurements are added to this diagram. The agreement between simulation and experiment is acceptable. The differences are of the same order of magnitude as for the validation (Figures 9 and 10).



Figure 11. (a) Comparison of simulated and measured data for surface residual stresses and (b) 1st zero-crossings.

For the 1st zero-crossing the influence of initial microstructure is significant. The comparison with measured data is again acceptable.

4.5. Comparison of Laser Hardening with Grind and Induction Hardening

Figure 12 compares the simulated data for laser hardening with simulated data for induction and grind hardening of QT_{30HRC} . Obviously, the laser hardening results reveal for the residual stress on the surface considerable higher compressive stresses than for grind hardening. The values for the 1st zero-crossing are in the investigated range for both surface heat sources almost equal. The depth range of induction hardening, on the other hand, is significantly larger, which is due to the generation of heat by eddy currents below the surface.



Figure 12. Comparison of calculated target quantities for induction, grind, and laser hardening plotted over (**a**) $(grad \theta)_{max}$ for the residual stresses at the surface and (**b**) over $\Delta \theta_{max}^2 \cdot \sqrt{t_c}$ for the 1st zero-crossing. The simulations are performed with the QT_{30HRC} variant as initial microstructure [7].

5. Discussion

In general, thermal loads can lead to modifications such as residual stresses, if permanent density changes, plasticity due to yield strength exceeding, and transformation induced plasticity occur. Essentially, the results are expected. Because of the hardening and the accompanied lower density of martensite compressive stresses occur at the surface for all three investigated processes. If the maximal temperature is significantly above A_{C3} , the depth of the hardening increases for higher maximal temperature and for slower processes. All three processes can be traced back qualitative to the same mechanisms. Quantitative very different values of the same internal material loads occur which cause similar mechanisms. These differences are the content and reason for considering process signatures.

5.1. Residual Stress at the Surface

Figure 12a presents simulated residual stress values for the surface for induction, grind, and laser hardening plotted over $(grad \theta)_{max}$.

The maximal temperature gradient seems to be a very important parameter for the evolution of the residual stresses. The explanation for that observation is that the temperature gradient generates strains due to thermal expansion. If the resulting stresses exceed the yield strength plastic deformation is generated.

In case of induction heating only low gradients can be generated. The reason for that behavior is the volume heating. The power is distributed over a relatively large area. Because of this the maximal temperature gradient occurs below the surface and cannot increase very much [7]. In case of surface heating the maximal gradient occur on the surface [7]. It depends on the quotient of applied heat flux density and heat conductivity \dot{q}/λ . In comparison to induction heating these values can be significantly higher.

Although heating by grinding and laser are both surface heating methods, the residual stress at the surface differ significantly (Figure 12a). There are two main differences between both processes. These are the power profile and the Peclet number. The power profile of the grinding process is assumed to be constant during the contact of sample surface and grinding wheel. The power profile of the laser process is only constant perpendicular to the moving direction. In moving direction it has a Gaussian profile (Figure 7).

The Peclet numbers of the grind hardening process are higher than 1, whereas for laser hardening the values do not exceed 0.7. To investigate the influence of these factors on the target quantities three different cases with two different power profiles and two different Peclet numbers are investigated (Table 3). The power profiles of these three cases are

presented in Figure 13. The Peclet number of the grinding example in Table 3 is 1.87. The Peclet number of the presented laser heating process is considerablly lower and equals 0.15.

Table 3. Comparison of different power profiles and Peclet numbers.

	Process	Laser	Laser	Grinding
1	Power profile	Gaussian	rectangular	rectangular
2	v _{ft} [mm/s]	4	4	20
3	lg [mm]	1.5	1.5	4
4	t_c [s]	0.4	0.4	0.2
5	Pe [-]	0.148	0.148	1.87
6	\dot{q}_{eq} [W/mm ²] (Equation (6))	26.00	24.00	25.71
7	max. <i>q̇</i> [W/mm ²]	29.31	24.00	25.71
8	θ_{max} [°C]	1110	1118	1084
9	$(grad \ \theta)_{max} \ [K/mm]$	1203	1326	1376
10	Surface $\varepsilon_{\text{pl II}}$ [-] (end of process)	0.00068	0.00066	-0.0008
11	Surface σ_{II} [MPa] (end of process)	-637	-606	-437



Figure 13. Power profiles of the three different heating processes presented in Table 3.

For comparison reasons the processes have similar power densities. Hence the generated maximal temperature gradients differ not too much (173 K/mm in row 9 of Table 3). The width of the sources l_g and the related heat source velocities lead to different Peclet numbers but to similar maximal temperature on the surface (row 8).

A comparison of the residual stresses simulated with the low Peclet number of 0.15 reveals only small differences (row 11). In addition, the plastic strains on the surface are almost equal (row 10). Therefore, it can be assumed that the power profile has in the range of the investigated process parameters only a slight influence on the evolution of the residual stresses at the surface.

However, the grinding process with higher Peclet number reveals significant differences to the results with low Peclet numbers. The compressive residual stress on the surface is considerably lower than in case of processes with lower Peclet numbers. The reason is probably the different development of the negative plastic strains during the heating period. As shown in Figure 14 the maximal negative plastic deformation during the heating period is much larger in case of high Peclet number.



Figure 14. Temperature evolution and generated plastic strains for laser hardening with a Gaussian power profile (**a**), laser hardening with a rectangular power profile (**b**), and a grind hardening with a rectangular power profile (**c**) (Figure 13 and Table 3), black dashed lines indicate begin and end of the power impact, grey dashed lines indicated the minimal and maximal axial plastic strains in case of low Peclet number PE 0.15.

In case of low Peclet number a preceding temperature front occurs [27]. It is visible in Figure 14 on the left (a) and middle picture (b). Before the active heating starts (indicated by the dashed lines) the surface temperature has reached values of about 800 °C. Because of this preheating the first plastic strains are not observable until high temperatures of several hundred °C are reached. The resulting maximal negative plastic strain is about -0.004. In case of the high Peclet number (Figure 14c) no significant preceding temperature front is detectable [26]. The plastic deformation begins at relative low temperatures and reaches significant larger values of about -0.01. Further on the negative plastic strain is in case of high Peclet numbers. However, the maximal positive plastic strain is in case of high Peclet numbers lower than in case of low Peclet numbers. For low Peclet numbers this behavior leads at the end of the processes to positive plastic strains and negative values in case of high Peclet numbers (row 10 in Table 3). The positive plastic strains add up to the positive transformation strains due to the martensite formation [28] and increase the compressive residual stresses. In case of the high Peclet number the negative plastic strain decreases the total strain and hence decreases the compressive stresses.

Therefore, the lower the temperature at which plastic deformation is generated by thermal expansion, the lower the surface compressive residual stresses at the end of the process. With other words, the strains during the heating period have a large influence on the stresses at the end of the process. A similar behavior is found for induction heating. If the material is preheated up to several hundred degrees higher compressive stresses of 200 MPa are found [29].

This behavior can be explained by the temperature differences inside the material. The lower the temperature differences the lower are the thermal gradients. Because of the fixation (Section 3.6) the workpiece cannot bend; the temperature gradient must generate elastic strains. For generating plastic deformation, a specific value of elastic strain, which corresponds to the yield limit, is necessary. To reach this strain the difference of the temperatures inside the workpiece must be high enough. Figure 15 presents thermal gradients of two cases with a low and a higher Peclet number at approximately 800 °C during the heating period. At the surface, the gradients are nearly equal. However, in deeper areas they differ significantly. The preheated case with a low Peclet number reveals inside the material lower thermal gradients than the not preheated sample with the high Peclet number. The generated elastic strains are therefore lower and hence the generated plastic deformation should be lower too.

Although the measured compressive residual stresses on the surface are systematically lower than the calculated data, they support the hypotheses of the influence of the Peclet number (Figure 16). The differences are larger in case of high thermal gradients. For lower



Figure 15. Temperature gradients for Pe = 0.15 (blue) and Pe = 1.86 (red) during the heating period at approximately 800 °C on the surface.



Figure 16. Residual stress on the surface. Simulation study for grind and laser hardening and comparison with measured data for laser hardening.

The reason for the systematic deviation to lower values is not fully understood. Maybe relaxation processes reduce the stresses [30]. The observation that the measured residual stresses are lower than the calculated data is also overserved by other authors [17,25] for laser and induction hardening.

5.2. The 1st Zero-Crossing of the Residual Stress Distribution

Figure 12b compares the depth of the 1st zero-crossings of the three different processes. For induction heating the depth is higher because the heat distribution inside the material is not only generated by thermal diffusion but also due to eddy currents (volume heating). In case of surface heating processes such as grinding and laser heating the values are nearly the same.

The load descriptive $\Delta \theta_{max}^2 \cdot \sqrt{t_c}$ was found more or less by trial and error. A stringent physical justification cannot be given up to now. As mentioned before, the depth of the 1st zero-crossing of the residual stress curve and the depth of 50% martensite amount are very similar [7]. One reason for that is that the martensite density is significantly lower than for all other phases [28] and promotes therefore compressive stresses within the martensite phase. This means that the depth of the 1st zero-crossing correlates with the transformation kinetic from initial microstructure to austenite, because only generated austenite can generate later on martensite during the quenching period. In the range of laser hardening conditions presented here, very fast self-cooling processes occur. Therefore it can be assumed that the complete amount of generated austenite transforms later on to martensite.

The transformation to austenite depends on alloying elements, initial microstructure and the heating rate [21]. In case of martensitic hardened but not tempered steel, a high amount of carbon is dissolved within the crystal matrix. A_{C1} is rather low in that case. The carbon distribution changes if a tempering process is applied to the material. The higher the tempering temperature and the tempering time the more carbide which bound the carbon will be formed [31]. Hence A_{C1} increases in comparison to the initial not tempered martensitic structure.

For common steels, the formation of austenite occurs in the temperature range between 750 and 950 °C. In Figure 12b this effect can be seen for the quenched and tempered variants. QT_{47HRC} has a lower A_{C1} temperature than QT_{30HRC} . Therefore the 1st zero-crossing is deeper in case of the QT_{47HRC} variant.

However, the chosen descriptive value $\Delta \theta_{max}^2 \cdot \sqrt{t_c}$ includes not any hints on the described transformation mechanism. Therefore it is likely that this descriptive value has restrictions and is only valid for a limited range of process and material parameters. Therefore it seems necessary to find an alternative descriptive value for the 1st zero-crossing.

For that reason, some considerations of the analytical solution of a moving heat source will be given [27]. Although the analytical solution of the moving heat source considers not any phase transformation and not any external cooling [27], this solution can give hints at which position a defined temperature (for example a transformation temperature) can occur. The analytical solution considers a half infinite work piece, for a material with temperature independent thermal parameters (heat capacity, thermal conductivity, and density).

Table 4 and Figure 17 present depth of a characteristic temperature (here 850 °C) calculated under the conditions of the analytical solution [27]. The temperature diffusivity κ is assumed to be 10.7 mm²/s. The width of the moving heat source l_g is set to 5 mm for all cases. The heating conditions are chosen in that way that the surface temperature reaches approximately 1000 °C, 1100 °C, and 1200 °C. The Peclet numbers vary from 0.1 to 10. Figure 17a presents the results and compares the calculated depth for reaching 850 °C as a function of $\Delta \theta_{max}^2 \cdot \sqrt{t_c}$. It can be seen that this plot depends still on temperature. Therefore, the maximal temperature is not completely eliminated by the chosen load characterization.

<i>q̇</i> [W/mm ²]	v _{ft} [mm/s]	<i>t</i> _c [s]	Peclet [-]	$\dot{q} \sqrt{t_c}$ [Ws ^{1/2} /mm ²]	θ_{max} [°C]	grad θ [K/mm]	Depth of 850 °C [mm]
41.47	86	0.06	10.0	10.00	1093	-1378	0.21
29.83	43	0.12	5.0	10.17	1094	-991	0.29
21.67	21.5	0.23	2.5	10.45	1093	-721	0.41
19.60	17.2	0.29	2.0	10.57	1092	-652	0.46
17.25	12.9	0.39	1.5	10.74	1093	-574	0.53
14.54	8.6	0.58	1.0	11.09	1091	-484	0.62
13.93	7.74	0.65	0.9	11.19	1092	-464	0.65
13.27	6.88	0.73	0.8	11.32	1091	-442	0.68
12.61	6.02	0.83	0.7	11.49	1094	-420	0.73
11.86	5.16	0.97	0.6	11.68	1091	-396	0.77
11.07	4.3	1.16	0.5	11.93	1091	-369	0.83
10.18	3.44	1.45	0.4	12.27	1090	-340	0.90
9.22	2.58	1.94	0.3	12.84	1093	-307	1.02
8.07	1.72	2.91	0.2	13.75	1095	-269	1.20
6.44	0.86	5.81	0.1	15.52	1099	-214	1.60

Table 4. Maximal temperature on surface and depth for reaching 850 °C calculated according the analytical solution of a moving heat source for Peclet numbers between 0.1 and 10.



Figure 17. The depth of 850 °C is reached for different maximal temperature at the surface, calculated by the analytic solution of a moving heat source, (**a**) plotted over the descriptive value $\Delta \theta_{max}^2 \cdot \sqrt{t_c}$, (**b**) plotted over the descriptive value ($\Theta_{max} - 850$ °C) $\sqrt{t_c}$.

On the right hand side in Figure 17b a modified descriptive value is chosen: (θ_{max} -850 °C) $\sqrt{(t_c)}$. By use of this parameter as load descriptive the depth of 850 °C depends not explicitly on the maximal temperature. This approach can be generalized by using a material and initial microstructure dependent start temperature of austenite formation instead of 850 °C. The depth of 1st zero-crossing Z_1 should correlate with the temperature at which 50% of the initial microstructure is transformed to austenite. This temperature is estimated with $\theta_t = (A_{C1} + A_{C3})/2$ with start and end temperature of austenite formation. Hence the proposal for a new descriptive load value is given by:

$$Z_1 = \left(\Theta_{max} - \frac{A_{C1} + A_{C3}}{2}\right) \cdot \sqrt{t_c} \tag{7}$$

5.3. Comparison with Measured 1st Zero-Crossings for Different Initial Microstructures

The mean value of A_{C1} and A_{C3} for the quenched and tempered material with a hardness of 47 HRC is 790 °C, and 830 °C for the material with 30 HRC [7]. The values are valid for a heating rate of approximately 1000 K/s. The plot of the measured 1st zero-crossing over the new proposed descriptive value given in equation (5) reveals a very good agreement to the simulated values (Figure 18). The agreement is significantly better than in Figure 11b. For higher values of Equation (5) the grinding and the laser hardening process split up.



Figure 18. 1st zero-crossing: Simulation study for grind and laser hardening and comparison with measured data for grind and laser hardening.

6. Conclusions

Correlations between internal material loads, process quantities, and material modifications provide a possibility to engineer the workpiece surface layer properties in a knowledge-based way. In this paper, laser hardening was investigated and compared with induction and grind hardening. The following conclusions can be given:

- If a specific residual stress at the surface and a specific zero-crossing is sought, the necessary internal material loads and with the availability of process models-the necessary process parameters are determinable. This this determinability is the fundamental goal of Process Signatures.
- Process Signature components for the target quantities "residual stress on the heated surface" and the "1st zero-crossing of the residual stress profile" are elaborated for laser hardening.
- The investigations presented here extend the description of the Process Signature. It
 includes the influence of the initial microstructure and correlates the modifications
 with associated physical properties such as thermal conductivity and phase kinetics.
- The maximal temperature θ_{max} , the maximal temperature gradient perpendicular to the heated surface $(grad \ \theta)_{max}$, and the heating time t_c are parameters for the descriptive values of the considered target quantities.
- The formulation of the descriptive values is mechanism orientated. The temperature gradient $(grad \ \theta)_{max}$ is a measure for the strains inside the material. It influences the residual stress on the surface. $(A_{C1} + A_{C3})/2$ is important for the 1st zero-crossing, because it indicates the mean temperature for austenitizing. In combination with Θ_{max} and $\sqrt{t_c}$ it describes the depth of the 1st zero-crossing.
- The target quantities depend additionally on the applied Peclet number of the moving heat source. It shows that the initial temperature is an important factor for heat treatment with moving heat sources.

Author Contributions: Conceptualization, F.F. and T.L.; methodology, F.F. and T.L.; validation, F.F. and Y.L.; formal analysis, F.F.; investigation, F.F.; resources, T.R.; writing—original draft preparation, F.F. and Y.L.; writing—review and editing, T.L., T.R., Y.L. and F.F.; project administration, T.L. and T.R.; funding acquisition, T.L. All authors have read and agreed to the published version of the manuscript.

Funding: This research was funded by German Research Foundation (DFG), grant number 223500200-TRR136.

Institutional Review Board Statement: Not applicable.

Informed Consent Statement: Not applicable.

Acknowledgments: The authors gratefully acknowledge the financial support of the research work by the German Research Foundation (DFG) within the transregional Collaborative Research Center SFB/TRR 136 "Process Signatures" sub project M01 and F07. Project-ID 223500200–TRR 136.

Conflicts of Interest: The authors declare no conflict of interest.

References

- 1. Field, M.; Kahles, J.F. Review of Surface Integrity of Machined Components. CIRP Ann. Manuf. Technol. 1971, 20, 153–163.
- Jawahir, I.; Brinksmeier, E.; M'Saoubi, R.; Aspinwall, D.; Outeiro, J.; Meyer, D.; Umbrello, D.; Jayal, A. Surface integrity in material removal processes: Recent advances. CIRP Ann. 2011, 60, 603–626. [CrossRef]
- 3. Habschied, M.; De Graaff, B.; Klumpp, A.; Schulze, V. Fertigung und Eigenspannungen. *HTM J. Heat Treat. Mater.* 2015, 70, 111–121. [CrossRef]
- 4. De Lacalle, L.N.L.; Lamikiz, A.; Salgado, M.A.; Herranz, S.; Rivero, A.; De Lacalle, L.N.L.; Mentxaka, A.L. Process planning for reliable high-speed machining of moulds. *Int. J. Prod. Res.* **2002**, *40*, 2789–2809. [CrossRef]
- Brinksmeier, E.; Klocke, F.; Lucca, D.A.; Sölter, J.; Meyer, D. Process Signatures—A New Approach to Solve the Inverse Surface Integrity Problem in Machining Processes. *Procedia CIRP* 2014, 13, 429–434. [CrossRef]
- 6. Karima, M.; Chandrasekaran, N.; Tse, W. Process signatures in metal stamping: Basic concepts. *J. Mater. Shap. Technol.* **1989**, *7*, 169–183. [CrossRef]

- 7. Frerichs, F.; Lübben, T. Development of Process Signatures for Manufacturing Processes with Thermal Loads without and with hardening. *Procedia CIRP* 2018, 71, 418–423. [CrossRef]
- 8. Borki, A.K.; El Ouafi, A.; Chebak, A.; Ouafi, E.; El Ouafi, A. Experimental Investigation of Laser Surface Transformation Hardening of 4340 Steel Spur Gears. J. Manuf. Mater. Process. 2019, 3, 72. [CrossRef]
- 9. Barka, N.; Karganroudi, S.S.; Fakir, R.; Thibeault, P.; Kemda, V.B.F. Effects of Laser Hardening Process Parameters on Hardness Profile of 4340 Steel Spline—An Experimental Approach. *Coatings* **2020**, *10*, 342. [CrossRef]
- 10. Vollertsen, F.; Partes, K.; Meijer, J. State of the art of Laser Hardening and Cladding. In *Lasers in Manufacturing*; Beyer, E., Dausinger, F., Ostendorf, A., Otto, A., Eds.; AT-Fachverlag Stuttgart: Fellbach, Germany, 2005; pp. 281–305.
- Nath, A.; Sarkar, S. Laser Transformation Hardening of Steel. In *Advances in Laser Materials Processing*; Elsevier BV: Amsterdam, The Netherlands, 2018; pp. 257–298.
- 12. Klocke, F.; Schulz, M.; Gräfe, S. Optimization of the Laser Hardening Process by Adapting the Intensity Distribution to Generate a Top-hat Temperature Distribution Using Freeform Optics. *Coatings* **2017**, *7*, 77. [CrossRef]
- 13. Martínez, S.; Lamikiz, A.; Ukar, E.; Calleja, A.; Arrizubieta, J.; de Lacalle, L.L. Analysis of the regimes in the scanner-based laser hardening process. *Opt. Lasers Eng.* **2017**, *90*, 72–80. [CrossRef]
- 14. Maamri, I.; Barka, N.; El Ouafi, A. ANN Laser Hardening Quality Modeling Using Geometrical and Punctual Characterizing Approaches. *Coatings* **2018**, *8*, 226. [CrossRef]
- 15. Volpp, J.; Dewi, H.S.; Fischer, A.; Niendorf, T. Influence of complex geometries on the properties of laser-hardened surfaces. *Int. J. Adv. Manuf. Technol.* **2020**, 107, 4255–4260. [CrossRef]
- 16. Frerichs, F.; Lübben, T. Influence of Initial Microstructure on Manufacturing Processes with Thermal Loads accompanied by Hardening. *Procedia CIRP* 2020, *87*, 521–526. [CrossRef]
- 17. Kostov, W. Untersuchungen zur Zeitaufgelösten Spannungsentwicklung und Eigenspannungsentstehung beim Laserstrahlstandhärten am Beispiel des Stahls 42CrMo4. Ph.D. Thesis, University of Karlsruhe, Karlsruhe, Germany, 2014.
- Muzik, T. Laser hardening of gear wheels well proven laser application for heavy industry. *Int. Congr. Appl. Lasers Electro Opt.* 2015, 2015, 136. [CrossRef]
- 19. Nemecek, S.; Muzik, T.; Misek, M. Laser hardening of gear wheels. Int. Congr. Appl. Lasers Electro Opt. 2012, 2012, 411. [CrossRef]
- Borchers, F.; Clausen, B.; Eckert, S.; Ehle, L.; Epp, J.; Harst, S.; Hettig, M.; Klink, A.; Kohls, E.; Meyer, H.; et al. Comparison of Different Manufacturing Processes of AISI 4140 Steel with Regard to Surface Modification and Its Influencing Depth. *Metals* 2020, 10, 895. [CrossRef]
- Miokovic, T. Analyse des Umwandlungsverhaltens bei ein- und Mehrfacher Kurzzeithärtung bzw. Laserstrahlhärtung des Stahls 42CrMo4. Ph.D. Thesis, University of Karlsruhe (TH), Karlsruhe, Germany, 2005; pp. 50–51.
- 22. Wildau, M.; Hougardy, H. Influence of the MS-temperature on stresses and changes in dimension. HTM 1987, 42, 261–268.
- Blondeau, P.; Maynier, J.; Dollet, J. Prévision de la dureté et de la résistance des aciers au carbone et faiblement alliés d'après leur structure et leur composition. *Mem. Sci. Rev. Métallurgie* 1973, 70, 883–892.
- 24. Epp, J. Materials Characterization Using Nondestructive Evaluation (NDE) Methods; Huebschen, G., Altpeter, I., Tschuncky, R., Herrmann, H.-G., Eds.; Woodhead Publishing: Cambridge, UK, 2016; pp. 81–124. ISBN 978-0-08-1000403.
- 25. Moore, M.G.; Evans, W.P. SAE Technical Paper 580035; SAE International: Warrendale, PA, USA, 1958; Volume 66, pp. 340-345.
- 26. Schwenk, M. Entwicklung und Validierung eines Numerischen Simulationsmodells zur Beschreibung der Induktiven Ein- und Zweifrequenzrandschichthärtung am Beispiel von Vergütetem 42CrMo4. Ph.D. Thesis, University of Karlsruhe, Karlsruhe, Germany, 2012.
- 27. Johnson, K.L. Contacts Mechanics; Cambridge University Press: Cambridge, UK, 1985; p. 379.
- 28. Lement, B.S. Distortion in Tool Steels; ASM Metal Park: Novelty, OH, USA, 1959.
- Schulz, A.; Cui, C.; Nadolski, D.; Steinbacher, M. Evaluierung des Potenzials Mechanisch Aufgeprägter oder Thermisch Induzierter Dehnungen zur Kontrolle des Eigenspannungszustandes einer Induktiv Gehärteten Randschicht ("Stress Hardening"); IGF-Projekt 18265 N; IGF-Projekt: Bremen, Germany, 2017.
- 30. Frerichs, F.; Meyer, H.; Strunk, R.; Kolkwitz, B.; Epp, J. Development of a Process Signature for Manufacturing Processes with Thermal Loads. *Met. Mater. Trans. A* 2018, 49, 3419–3429. [CrossRef]
- 31. Kaiser, D.; De Graaff, B.; Dietrich, S.; Schulze, V. Investigation of the precipitation kinetics and microstructure evolution of martensitic AISI 4140 steel during tempering with high heating rates. *Met. Res. Technol.* **2018**, *115*, 404. [CrossRef]