

International journal
for science, technics and
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MACHINES
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MATERIALS

YEAR XIX **Issue 1** / 2025 **ISSN PRINT 1313-0226**
ISSN WEB 1314-507X



Published by
Scientific technical
Union of Mechanical Engineering



MACHINES. TECHNOLOGIES. MATERIALS

INTERNATIONAL SCIENTIFIC JOURNAL

ISSN PRINT 1313-0226, ISSN WEB 1314-507X, YEAR XIX, ISSUE 1 / 2025

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A validation approach for a FEA model of thermal distortion for CO-CR thin wall structures produced by LPBF process

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Abstract: Laser Powder Bed Fusion (LPBF) is a prominent additive manufacturing process used for fabricating complex metallic structures, but it often encounters challenges related to thermal distortions and residual stresses, particularly in thin-walled structures. These issues compromise the integrity and dimensional accuracy of the parts. Finite Element Analysis (FEA) has been essential in simulating and understanding the thermal and mechanical behaviors during the LPBF process. This study focuses on validating a refined FEA model developed using ANSYS Additive Print (AAP) to predict the thermal distortions in CoCr thin-wall structures. The validation involves comparing simulation results with experimental data to verify the model's effectiveness. The study demonstrates the integration of advanced simulation techniques in predicting distortions and stresses, thereby enhancing the reliability and accuracy of the manufacturing process.

Keywords: LPBF, Additive Manufacturing, ANSYS, FEA Analysis, Structural analysis, Distortion

1. Introduction

Laser Powder Bed Fusion (LPBF) today is one of the most employed and popular additive manufacturing processes, known for its ability to fabricate complex metallic structures that are otherwise almost impossible to manufacture. However, the process often counters problems due to the thermal distortions and residual stresses, particularly in thin-walled structures, which pose challenges to the integrity and dimensional accuracy of the parts produced. To combat these issues, Finite Element Analysis (FEA) has been pivotal in simulating and understanding the thermal and mechanical behaviors during the LPBF. Recent advances have seen the development of sophisticated FEA models that incorporate detailed layer-by-layer simulation and material solidification dynamics, aiming to enhance the predictive accuracy of thermal distortions [1, 2]. An analytical-numerical approach to distortion analysis in thin-walled structures was previously analysed for welded structures [3] and laminated composite structures [4]. The validation of these models using experimental data is crucial not only for verifying their effectiveness but also for refining the simulation parameters used to predict distortions and optimize the manufacturing process [5].

The exploration of ANSYS as a well known FEA solution, is extensive especially in the context of 3D printing and additive manufacturing. One study [6] emphasizes the integration of finite element analysis (FEA) with ANSYS software to enhance the design and functionality of 3D printing devices, highlighting a dual-operation system for both milling and printing. Another critical approach involves topology optimization within ANSYS to refine the design of additively manufactured components, addressing overhang structures that are challenging to produce [7]. There is also innovative research on estimating the success probability of 3D prints using a centralized computer system that analyzes model features [8]. ANSYS Additive Print (AAP) is also used in predicting thermal distortions in the additive manufacturing processes. The literature highlights various approaches, such as the development of thermal circuit network models specifically designed to predict thermal distortions efficiently by estimating part temperature history during the metal powder bed fusion process, with significantly faster computational speeds than traditional methods [9]. Another study demonstrates the simulation of thermal stress using ANSYS on specific materials like 316L, focusing on the thermal stress during welding processes which significantly affect the distortion and thermal behavior of the material [10]. AAP is reported to generate good results in simulating residual deformations that is an outcome of these thermal changes [11].

This study elaborates on the methods and results of validating a refined FEA model developed using ANSYS Additive Print that is tailored to predict thermal distortions in thin-wall structures produced by LPBF, setting the foundation for more reliable and accurate manufacturing outcomes.

2. Method

2.1 Material

LPBF process for metals uses powders of numerous different metals and alloys, Cobalt-Chromium (CoCr) being one of the most common ones. CoCr powders are employed especially in applications requiring high strength, wear resistance, and corrosion resistance. CoCr alloys typically consist of cobalt as the base element with chromium as a significant alloying element. Other elements such as molybdenum, tungsten, and nickel can also be present in smaller amounts to enhance specific properties. The high chromium content provides excellent corrosion resistance, making CoCr alloys suitable for applications in harsh environments, including medical and dental implants. [12, 13]

The powder used in LPBF typically has a particle size distribution optimized for the process, often in the range of 15-53 micrometers. The distribution affects powder flowability and packing density, which in turn influences the quality of the printed part. High purity of the powder is essential to ensure good mechanical properties and avoid defects in the printed parts. Impurities can cause porosity and other defects that compromise the integrity of the final part. [13, 14]

The metallic CoCr powder manufactured by company Sentas-Bir (Kemalpaşa, Izmir) has been used for producing the samples for this research. This powder that is produced with gas atomisation, has 18-47 µm particle distribution and almost spherical shape. Elemental spectroscopy analysis values for its elemental combination are given in Table 1.

Table 1: Elemental Analysis of the CoCr powder used in validation tests

Element	(wt%)
Cr	23,3±0,5
Si	0,89±0,01
Mn	0,220±0,002
Mo	5,47±0,02
W	5,33±0,07
Co	Remaining

2.2 Test Machine for Producing the LPBF Validation Samples

A LPBF prototype machine that is developed by Laseral Ltd. research department and Ege University research team has been used in this work. The system has an open architecture software and all process parameters can freely be changed for research purposes (see Fig. 1.a). System has a 1064 nm 200 W CW fiber laser source and Ø 90 mm part processing area with 90 mm height. System can also be operated with a Ø 30 mm substrate in order to save powder when manufacturing small samples (see Fig. 1.b). In order to create measurable distortions and detect them easily, thin wall structures

with varying thickness have been selected as an ideal geometry (see Fig. 1.c).

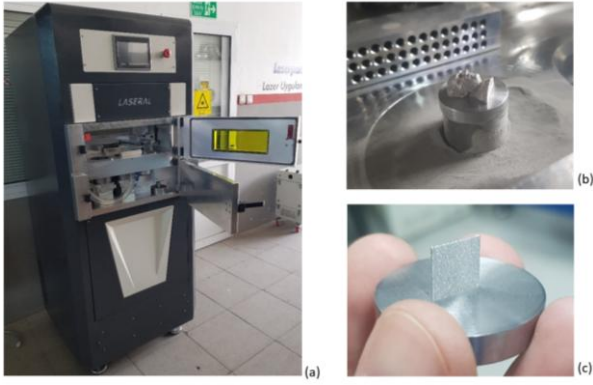


Fig. 1 (a) LPBF prototype machine developed by Laseral Ltd.; **(b)** Sample 3D geometry produced with this machine; **(c)** Sample thin wall geometry produced for validation

2.3 FEA Simulation Model

Ansys Additive Print module of the Ansys Additive 2021R1 is used to construct the simulation models in this research. ANSYS Additive Print (AAP) is a specialized software tool within the ANSYS suite designed to simulate and optimize additive manufacturing (AM) processes, specifically focusing on 3D printing with metals. It aims to provide users with predictive insights into the additive manufacturing process, enabling them to identify and mitigate potential issues before physical printing. This helps in improving the quality of printed parts, reducing material waste, and minimizing the need for post-processing. [15, 16]

AAP simulates the thermal and structural behavior of parts during the additive manufacturing process. This includes predicting temperature distributions, residual stresses, and distortions that occur due to the thermal cycles involved in metal 3D printing. The software models the build process layer by layer, capturing the effects of each layer's deposition on the overall part. [17, 18]. It is based on a limited number of material input parameters, namely elastic modulus and Poisson ratio. Yield strength and bilinear elastic-plastic deformation characteristics are required as well [19].

In AAP, the LPBF build process is simulated by layer-by-layer activation of voxel elements that initially have the same inherent strain. After each voxel layer is activated, the deformation at mechanical equilibrium is calculated. At the beginning, the initial inherent strain applied is assumed to be equal to the strain at the yield stress, i.e., σ_y/E , and must be calibrated against actually printed calibration geometries. This calibration inherently includes information about the machine-specific AM process and the material-specific response [11]. The software offers both purely elastic and elastic-plastic simulation options. In this work, the J2 plasticity model was used to more accurately predict the residual stress level and the resulting residual deformation. Additionally, there are three different inherent strain modes that can be employed in the simulation model, namely Assumed Strain, Scan Pattern and Thermal Strain. TS (Thermal Strain) simulation type is employed since the TS simulation type is the most accurate approach within AAP. This is because thermal ratcheting effects (accumulation of thermal strain due to local re-heating of already solidified layers) and inhomogeneous cooling are taken into account in this simulation type.

The key components of the ANSYS Additive Print simulation include the AP Mechanics Solver and the Thermal Solver since it is particularly focusing on thermal and mechanical effects. Governing mathematical equations of these solvers and the inherent strain modes are given in the related sections below.

2.3.1 AP Mechanics Solver

In the mechanics solver, mechanical and thermal strain are calculated from:

$$(1) \quad \varepsilon = \varepsilon_m + \varepsilon_t$$

where ε is the total strain, ε_m is the mechanical strain, ε_t is the thermal strain. Internal Force is calculated from:

$$(2) \quad F_{int} = B^T \cdot \sigma = 0$$

where B is the strain-displacement matrix, σ is the stress sensor. Equilibrium Equation is calculated from:

$$(3) \quad K \cdot u = F$$

where K is the global stiffness matrix, u is the global displacement vector, F is the global Force vector. Plasticity Model (Newton-Raphson Method) calculation is as follows:

$$(4) \quad f = \sigma - \sigma_y - H \cdot \varepsilon_p$$

where f is the yield function, σ_y is the yield strength, H is the hardening modulus, ε_p is the equivalent plastic strain. Residual and convergence for the model is calculated by these equations:

$$(5) \quad r = K \cdot u - F$$

$$(6) \quad \Delta u = K^{-1} \cdot r$$

2.3.2 Thermal Solver

Thermal solver heat transfer equation is:

$$(7) \quad \rho \cdot c_p \cdot \frac{\partial T}{\partial t} = \nabla \cdot (k \cdot \nabla T) + Q$$

where ρ is the density, c_p is the specific heat, T is the temperature, k is the thermal conductivity, Q is the internal heat generation. For the moving beam, Gaussian Laser Model is:

$$(8) \quad Q = \eta \cdot P \cdot \exp\left(-\frac{2r^2}{w^2}\right)$$

where η is the absorption coefficient, P is the laser power, r is the radial distance from the center of the laser beam, w is the laser beam radius. Boundary conditions are indicated with:

$$(9) \quad -k \cdot \nabla T \cdot \hat{n} = q$$

where $\hat{n}$ is the unit normal vector on the boundary surface and q is the heat flux on the boundary surface.

2.3.3 Inherent Strain Modes

Calculation of the Assumed Strain (isotropic) is from the below equation:

$$(10) \quad \varepsilon_{assumed} = SSF \cdot \frac{\sigma_y}{E}$$

where SSF is the strain scaling factor, σ_y is the yield strength and E is the elastic modulus.

Scan Pattern based anisotropic strain equation is:

$$(11) \quad \varepsilon_{scan} = \varepsilon_{inherent} \cdot ASF$$

where $\varepsilon_{inherent}$ is the inherent strain, ASF is the anisotropic strain coefficient.

Finally the employed strain mode, Thermal Strain is equal to the Thermal Ratcheting Algorithm output. Thermal Ratcheting Algorithm that is employed in the Thermal Strain simulation has the equation:

$$(12) \quad \varepsilon_t = \sum(\Delta \varepsilon_{ti})$$

Here, $\Delta \varepsilon_{ti}$ is the incremental thermal strain and strain accumulation occurs each time the temperature exceeds 40% of the material's absolute melting temperature. It has the equation:

$$(13) \quad \Delta \varepsilon_{ti} = \alpha \cdot (T - T_0)$$

where α is the coefficient of thermal expansion and T_0 is the initial temperature of the part.

2.3.4 Wall geometries produced in FEA model

Two different wall thicknesses were created for testing the model, 0.5 mm and 1.0 mm. 20 mm X 20 mm wall shapes were formed in the model with 0.5 mm voxel size, a voxel sampling rate of 5 and a mesh resolution factor of 5. On-plate displacement graphs have been obtained and the distortions along the horizontal cross section of sample geometries have been noted (see Fig. 2) in order to compare with the experimental LPBF sintered samples.

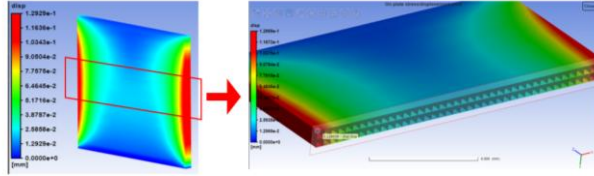


Fig. 2 On-plate displacement graph of thin wall geometry, inspected along a horizontal cross section

2.4 3D Measurements

All 3D measurements have been made by using a Zeiss Comet 5M 3D scanner. A point cloud is obtained using this device and taken into the computer in the form of an .STL file. STL file was processed using MeshLab software to form a part surface and filtering certain outliers by using a cleaning/repairing filter (see Fig. 3). The resulting data has been transferred to spreadsheet format in the form of X, Y and Z coordinates.

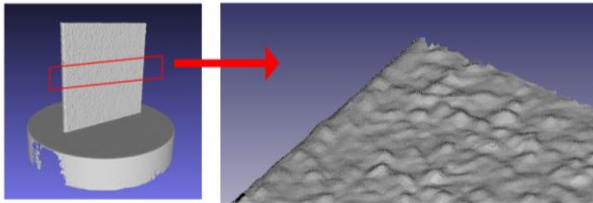
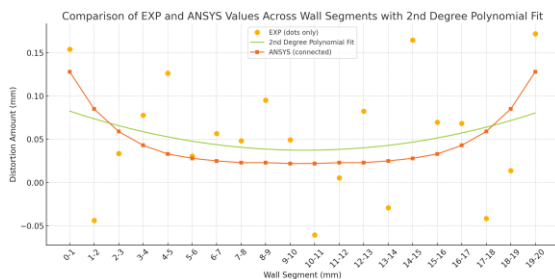


Fig. 3 Surface obtained from the point cloud using MeshLab and the slice of data that is picked from the middle of the wall shaped part

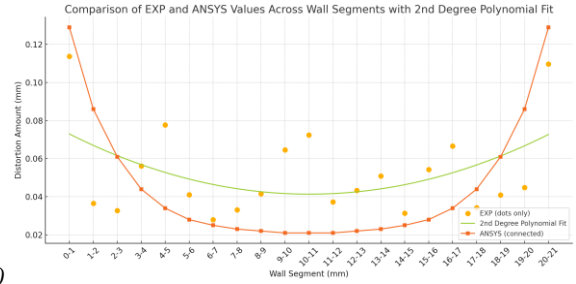
3. Results and Discussion

3.1 Model Validation

The results generated by the Ansys Additive Print simulation software has been compared to those obtained using the 3D measurements. The experimental data has a natural variation so a second order polynomial has been fitted along the 20 mm wall width as a trendline to observe the variation path. As this trendline is compared to predicted simulation data, it is seen that the model was able to predict distortions with a good approximation in both trials (see Fig 4).



(a)



(b)

Fig. 4 Comparison of experimental and predicted values for varying wall thicknesses (a) 0.5 mm, (b) 1.0 mm

3.2 Analysis for Varying Wall Thickness

Following the validation process, a second analysis has been performed, this time to see the effect of increasing wall thickness on the distortion level. Thickness of the wall has been increased multiple times sequentially as 0.5 mm, 1.0 mm, 2.0 mm and 4.0 mm. and displacement graphs have been obtained accordingly (see Fig. 5)

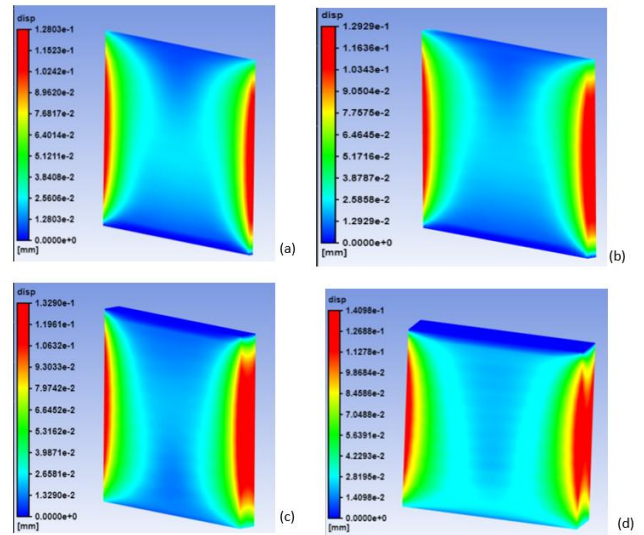


Fig. 5 Predicted distortions for varying wall thicknesses (a) 0.5 mm, (b) 1.0 mm, (c) 2.0 mm, (d) 4.0 mm

Maximum recorded distortion on each part has been recorded and put on a chart to see the relation in between (see Fig 6). It is seen that there is a very linear, defined relation between increasing wall thickness and the distortion amount. More total distortion on the corners of the part can be expected as more heat will be accumulated with increasing wall thickness. The function connecting distortion level 'D' to part thickness 'W' can be approximated with the equation $D = 0.00381 * W + 0.12561$ according to this analysis. This implies that as we increase the thickness of a sintered part feature manufactured with LPBF using the given parameter set in this work, we can roughly expect a distortion of around % 0.4 of the thickness of this feature on the corners of the wall structure.

4. Conclusion

The validation of the refined FEA model using ANSYS Additive Print has shown promising results in adequately predicting thermal distortions in CoCr thin-wall structures produced by LPBF. The comparison between the simulated data and experimental measurements indicates a good level of precision in the model's predictions. The study reveals a linear relationship between wall thickness and distortion levels, providing a functional approximation that can be used to anticipate deviations in

manufactured parts. By incorporating machine-specific and material-specific responses, the model offers a robust tool for optimizing LPBF processes. Future work will focus on further refining the model parameters and exploring its application to other materials and geometries to broaden its usability in additive manufacturing.

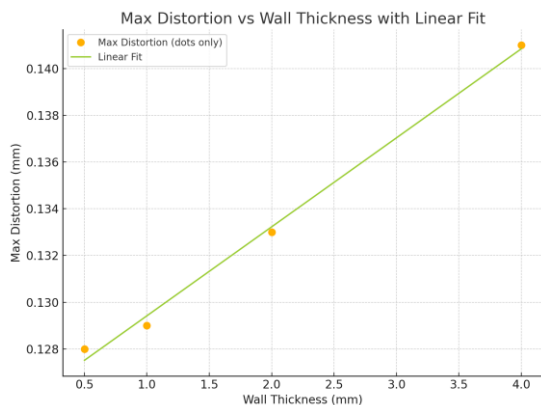


Fig. 6 Linear relation between wall thickness and maximum distortion of a thin wall shaped part

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Exergy analysis of 160 MW three cylinder steam turbine segments

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Abstract: Exergy analysis of three cylinder steam turbine segments is performed in this research. The highest mechanical power of 47389.66 kW is developed in the first segment (Seg. I, which actually represents the entire HPC – High Pressure Cylinder). Intermediate Pressure Cylinder (IPC) is the dominant mechanical power producer of all cylinders and it develops 48.95% of cumulative mechanical power produced in the whole turbine. The outlet Low Pressure Cylinder (LPC) segments (Seg. VII and IX) have the highest exergy destructions and the lowest exergy efficiency (equal to 61.27%) of all turbine segments. The best exergy performance shows IPC segments – Seg. V has the lowest exergy destruction (equal to 363.84 kW), while Seg. II has the highest exergy efficiency (equal to 94.04%) of all turbine segments. Outlet LPC segments (Seg. VII and IX) are the most sensitive to the ambient temperature change – their exergy efficiency decreases for 3.19% when the ambient temperature increases from 5 °C to 45 °C.

KEYWORDS: EXERGY ANALYSIS, COMPLEX STEAM TURBINE, TURBINE SEGMENTS, THE AMBIENT TEMPERATURE CHANGE

1. Introduction

Currently, the majority of electricity worldwide is produced by various kinds of steam turbines [1, 2]. However, steam turbines which can be found in power plants can also be used for many other purposes, the electricity production is a dominant, but not the only task which steam turbine can accomplish [3, 4].

In the conventional thermal or nuclear power plants are used high power steam turbines which are usually composed of several cylinders connected to the same shaft [5, 6]. Medium and low power steam turbines can be composed of one or two cylinders, what depends on the required mechanical power production [7, 8]. Turbine cylinders are usually composed of several turbine stages [9]. However, in exploitation can also be found auxiliary low power steam turbines which are composed of only one Curtis stage (such steam turbines are often used for the pump drive) [10].

Steam turbine cylinders can be single flow or dual flow cylinders, while dual flow cylinders can be symmetrical and dissymmetrical. In single flow turbine cylinder steam expands in just one direction, while in the dual flow cylinder steam expands simultaneously in two directions [11].

This paper presents exergy analysis of a three cylinder steam turbine segments. Such exergy analysis allows insight in the operation of different turbine cylinder parts, the cylinder is not considered as a whole (as in the standard exergy analysis). The observed steam turbine has nine segments overall – produced mechanical power, exergy efficiencies and exergy destructions are presented and compared for all segments. It is also analyzed and observed which turbine segment is the most sensitive to the ambient temperature change.

2. Description and operating regime of the analyzed steam turbine and turbine segments

Observed steam turbine consists of three cylinders which are connected to the same shaft: High Pressure Cylinder (HPC), Intermediate Pressure Cylinder (IPC) and Low Pressure Cylinder (LPC), Fig. 1. HPC and IPC are single flow cylinders, while LPC is a dual flow symmetrical cylinder, therefore both LPC parts (left part LPC-L and right part LPC-R) have the same operating parameters and performance. The shaft on which are connected all cylinders is used for the electric generator drive.

Exergy analysis is performed for all steam turbine segments (what requires division of each cylinder to the segments). Cylinder segment is a part of each cylinder defined between the cylinder inlet, outlet and steam extractions. Each turbine cylinder division to the segments allow insight into the operation performance of different cylinder parts, which cannot be found in the standard plant or turbine exergy analysis [12, 13].

HPC did not possess any steam extractions, therefore HPC has actually only one segment defined between HPC inlet and HPC outlet (HPC is its own segment, Seg. I).

IPC has three steam extractions, which allows IPC division on the four segments. The first IPC segment (Seg. II) is defined between IPC inlet and first IPC extraction (operating point 6, Fig. 1) which deliver a certain steam mass flow rate to the components of high pressure feedwater heating system. Second IPC segment (Seg. III) is defined between first and second IPC extractions (operating points 6 and 7, Fig. 1). Second IPC extraction delivers a certain steam mass flow rate to the deaerator. Third IPC segment (Seg. IV) is defined between second and third IPC extractions (operating points 7 and 8, Fig. 1), where third IPC extraction delivers a certain steam mass flow rate to the components of low pressure condensate heating system. The last, fourth IPC segment (Seg. V) is defined between third IPC extraction and IPC outlet (operating point 9, Fig. 1).

LPC is a dual flow cylinder – steam mass flow rate enters in the cylinder at its central part (operating point 9, Fig. 1) and simultaneously expands through both LPC parts (one half of the steam mass flow rate expands through LPC-L and other half expands through LPC-R). LPC-L and LPC-R have identical steam operating parameters at the inlet, in the extractions and at the outlet which confirms that LPC is symmetrical cylinder (steam temperature, pressure and mass flow rate are identical in operating points 10 and 11 and in operating points 13 and 14, Fig. 1). Each LPC part has two segments which are defined between LPC inlet and steam extraction and between steam extraction and LPC part outlet. For the LPC-L mentioned two segments are marked as Seg. VI and Seg. VII, while for the LPC-R segments are marked as Seg. VIII and Seg. IX. At the end of expansion in both LPC parts (operating points 13 and 14, Fig. 1), remaining steam mass flow rate is delivered to main condenser for condensation [14].

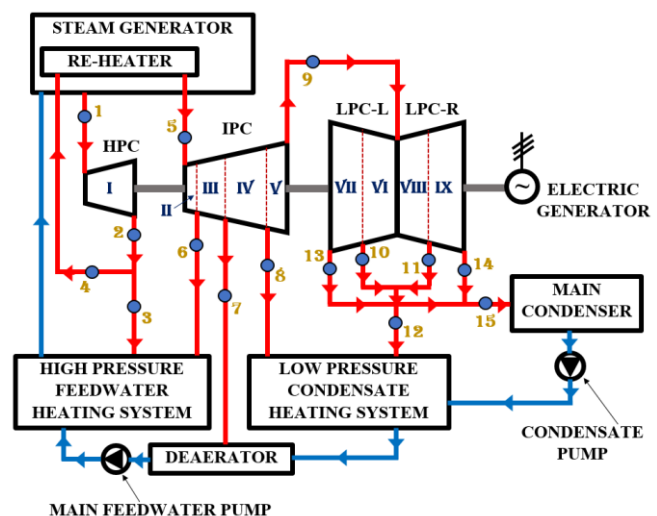


Fig. 1. General scheme of the three-cylinder 160 MW steam turbine along with operating points required for the exergy analysis and each cylinder marked segments (I – IX)

Steam expansion process in all cylinders and segments of the observed turbine in Specific enthalpy/Specific entropy (h/s) diagram is shown in Fig. 2. Exergy analysis of any steam turbine cylinder or segment requires data of the real (polytropic) steam expansion process [15]. Data of real expansion process can be properly obtained only from the measurements inside power plant during the exploitation, therefore these data involve all the losses which occur during operation. Losses in the real steam expansion process are proportional to the increase in steam specific entropy, as presented in Fig. 2, so steam specific entropy must continuously increase from the inlet until the outlet of each turbine cylinder or segment. Only in the ideal (isentropic) steam expansion process from the inlet until the outlet of each turbine cylinder or segment, steam specific entropy will remain constant, but the observation of such ideal expansion process is not required in the exergy analysis [16]. It should be highlighted that operating point's numeration and segments marking presented in Fig. 2 are in relation to operating point's numeration and segments marking from Fig. 1.

Steam expansion process is identical in both LPC parts (LPC-L and LPC-R) because LPC is a symmetrical dual flow cylinder, Fig. 2. At the LPC outlet (operating points 13, 14 and 15, Fig. 1 and Fig. 2) steam is slightly under the saturation curve (wet steam) which can be transferred to condensate in main condenser.

Fig. 2 is obtained from known steam operating parameters in each operating point (presented in Table 4) by using NIST-Refprop 9.0 software [17].

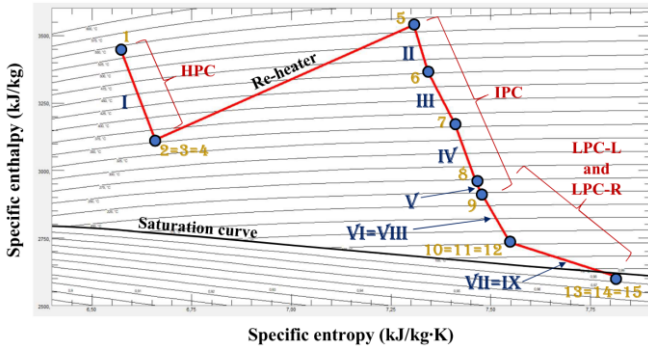


Fig. 2. Specific enthalpy/Specific entropy (h/s) diagram of steam expansion process throughout steam turbine and all its segments

3. Exergy analysis equations

In comparison to energy analysis which did not consider parameters of the ambient (ambient pressure and temperature), exergy analysis considers ambient parameters (parameters of the ambient in which observed system, component or component part operates). Second law of thermodynamics is a baseline for an exergy analysis [18, 19].

3.1. Overall equations, balances and principles

During the exergy analysis of any system, component or component part, a few overall equations, balances and principles must always be satisfied [7]. These equations, balances and principles will be presented in this subsection.

The overall exergy balance equation, valid for any system, component or component part at steady state is [9]:

$$\dot{X}_{\text{heat}} - P - \sum \dot{E}x_{\text{outlet}} + \sum \dot{E}x_{\text{inlet}} - \dot{E}x_{\text{Destruction}} = 0, \quad (1)$$

where P is mechanical power. $\dot{E}x$ is a fluid total exergy flow, which can be defined by an equation [8]:

$$\dot{E}x = \dot{m} \cdot \varepsilon, \quad (2)$$

where $\dot{m}$ is a fluid mass flow rate and ε is fluid specific exergy. Fluid specific exergy (ε) is defined by an equation [20]:

$$\varepsilon = (h - h_0) - T_0 \cdot (s - s_0), \quad (3)$$

where h is fluid specific enthalpy, s is fluid specific entropy, T is a temperature and index 0 is related to the ambient state. Exergy transfer by heat at the temperature T ($\dot{X}_{\text{heat}}$) is defined as [6]:

$$\dot{X}_{\text{heat}} = \sum (1 - \frac{T_0}{T}) \cdot \dot{Q}_{\text{heat}}, \quad (4)$$

where $\dot{Q}_{\text{heat}}$ is an energy transfer by heat. Mass flow rate balance for any system, component or component part is [4]:

$$\sum \dot{m}_{\text{inlet}} = \sum \dot{m}_{\text{outlet}}. \quad (5)$$

3.2. Equations for the exergy analysis of turbine segments

Equations for the exergy analysis of the observed turbine segments are defined according to the recommendations from the literature [21-23]. Indexes and segments marking applied in all equations from this subsection are related to the operating points and markings shown in Fig. 1 and Fig. 2. In Table 1 are presented equations for each turbine segment developed mechanical power calculation, while in Table 2 and Table 3 are presented equations for each turbine segment exergy inlet and exergy outlet calculation.

Table 1. Equations for each segment developed mechanical power calculation

Segment	Developed mechanical power	Eq.
I	$P_{\text{Seg.I}} = \dot{m}_1 \cdot (h_1 - h_2)$	(6)
II	$P_{\text{Seg.II}} = \dot{m}_5 \cdot (h_5 - h_6)$	(7)
III	$P_{\text{Seg.III}} = (\dot{m}_5 - \dot{m}_6) \cdot (h_6 - h_7)$	(8)
IV	$P_{\text{Seg.IV}} = (\dot{m}_5 - \dot{m}_6 - \dot{m}_7) \cdot (h_7 - h_8)$	(9)
V	$P_{\text{Seg.V}} = (\dot{m}_5 - \dot{m}_6 - \dot{m}_7 - \dot{m}_8) \cdot (h_8 - h_9)$	(10)
VI	$P_{\text{Seg.VI}} = \frac{\dot{m}_9}{2} \cdot (h_9 - h_{10})$	(11)
VII	$P_{\text{Seg.VII}} = (\frac{\dot{m}_9}{2} - \dot{m}_{10}) \cdot (h_{10} - h_{13})$	(12)
VIII	$P_{\text{Seg.VIII}} = \frac{\dot{m}_9}{2} \cdot (h_9 - h_{11})$	(13)
IX	$P_{\text{Seg.IX}} = (\frac{\dot{m}_9}{2} - \dot{m}_{11}) \cdot (h_{11} - h_{14})$	(14)

Table 2. Equations for each segment exergy inlet calculation

Segment	Exergy inlet	Eq.
I	$\dot{E}x_{\text{Seg.I,inlet}} = \dot{m}_1 \cdot \varepsilon_1$	(15)
II	$\dot{E}x_{\text{Seg.II,inlet}} = \dot{m}_5 \cdot \varepsilon_5$	(16)
III	$\dot{E}x_{\text{Seg.III,inlet}} = (\dot{m}_5 - \dot{m}_6) \cdot \varepsilon_6$	(17)
IV	$\dot{E}x_{\text{Seg.IV,inlet}} = (\dot{m}_5 - \dot{m}_6 - \dot{m}_7) \cdot \varepsilon_7$	(18)
V	$\dot{E}x_{\text{Seg.V,inlet}} = (\dot{m}_5 - \dot{m}_6 - \dot{m}_7 - \dot{m}_8) \cdot \varepsilon_8$	(19)
VI	$\dot{E}x_{\text{Seg.VI,inlet}} = \frac{\dot{m}_9}{2} \cdot \varepsilon_9$	(20)
VII	$\dot{E}x_{\text{Seg.VII,inlet}} = (\frac{\dot{m}_9}{2} - \dot{m}_{10}) \cdot \varepsilon_{10}$	(21)
VIII	$\dot{E}x_{\text{Seg.VIII,inlet}} = \frac{\dot{m}_9}{2} \cdot \varepsilon_9$	(22)
IX	$\dot{E}x_{\text{Seg.IX,inlet}} = (\frac{\dot{m}_9}{2} - \dot{m}_{11}) \cdot \varepsilon_{11}$	(23)

Table 3. Equations for each segment exergy outlet calculation

Segment	Exergy outlet	Eq.
I	$\dot{E}x_{\text{Seg.I,outlet}} = \dot{m}_2 \cdot \varepsilon_2 + P_{\text{Seg.I}}$	(24)
II	$\dot{E}x_{\text{Seg.II,outlet}} = \dot{m}_6 \cdot \varepsilon_6 + (\dot{m}_5 - \dot{m}_6) \cdot \varepsilon_6 + P_{\text{Seg.II}}$	(25)
III	$\dot{E}x_{\text{Seg.III,outlet}} = \dot{m}_7 \cdot \varepsilon_7 + (\dot{m}_5 - \dot{m}_6 - \dot{m}_7) \cdot \varepsilon_7 + P_{\text{Seg.III}}$	(26)
IV	$\dot{E}x_{\text{Seg.IV,outlet}} = \dot{m}_8 \cdot \varepsilon_8 + (\dot{m}_5 - \dot{m}_6 - \dot{m}_7 - \dot{m}_8) \cdot \varepsilon_8 + P_{\text{Seg.IV}}$	(27)
V	$\dot{E}x_{\text{Seg.V,outlet}} = \dot{m}_9 \cdot \varepsilon_9 + P_{\text{Seg.V}}$	(28)
VI	$\dot{E}x_{\text{Seg.VI,outlet}} = \dot{m}_{10} \cdot \varepsilon_{10} + (\frac{\dot{m}_9}{2} - \dot{m}_{10}) \cdot \varepsilon_{10} + P_{\text{Seg.VI}}$	(29)
VII	$\dot{E}x_{\text{Seg.VII,outlet}} = \dot{m}_{13} \cdot \varepsilon_{13} + P_{\text{Seg.VII}}$	(30)
VIII	$\dot{E}x_{\text{Seg.VIII,outlet}} = \dot{m}_{11} \cdot \varepsilon_{11} + (\frac{\dot{m}_9}{2} - \dot{m}_{11}) \cdot \varepsilon_{11} + P_{\text{Seg.VIII}}$	(31)
IX	$\dot{E}x_{\text{Seg.IX,outlet}} = \dot{m}_{14} \cdot \varepsilon_{14} + P_{\text{Seg.IX}}$	(32)

Exergy destruction of each turbine segment is calculated as:

$$\dot{E}x_{\text{Seg.}(i),\text{Destruction}} = \dot{E}x_{\text{Seg.}(i),\text{inlet}} - \dot{E}x_{\text{Seg.}(i),\text{outlet}} \quad (33)$$

Each turbine segment exergy efficiency is calculated as:

$$\eta_{\text{Seg.}(i),\text{EX}} = \frac{P_{\text{Seg.}(i)}}{\dot{E}x_{\text{Seg.}(i),\text{inlet}} - \dot{E}x_{\text{Seg.}(i),\text{outlet}} + P_{\text{Seg.}(i)}} \quad (34)$$

In Eq. 33 and Eq. 34 index i represents each turbine segment ($i = \text{I, II, III, IV, V, VI, VII, VIII, IX}$).

4. Steam operating parameters required for the turbine segments exergy analysis

Exergy analysis of each observed turbine segment is performed by using steam operating parameters measured in each operating point (Fig. 1 and Fig. 2) during the plant exploitation. These operating parameters are found in [24] and presented in Table 4. During the plant exploitation, in each operating point from Fig. 1 and Fig. 2 are measured steam temperature, pressure and mass flow rate - other steam operating parameters presented in Table 4 are calculated using NIST-Refprop 9.0 software [17].

As the exergy analysis considers parameters of the ambient (ambient temperature and pressure), in exergy analysis of any system, component or segment is required the definition of the base ambient state [25]. In this research, the base ambient state is defined by the ambient temperature of 25 °C and the ambient pressure of 1 bar.

Table 4. Steam operating parameters for the turbine segments exergy analysis

O. P. *	Temp. (°C)	Pressure (bar)	Mass flow rate (kg/s)	Specific enthalpy (kJ/kg)	Specific entropy (kJ/kg·K)	Specific exergy (kJ/kg) **
1	538	129.19	145.10	3440.6	6.5738	1485.20
2	355	36.33	145.10	3114.0	6.6586	1133.30
3	355	36.33	10.10	3114.0	6.6586	1133.30
4	355	36.33	135.00	3114.0	6.6586	1133.30
5	538	32.39	135.00	3540.3	7.3062	1366.60
6	452	18.04	7.60	3365.2	7.3429	1180.40
7	356	8.37	7.44	3174.1	7.4091	969.63
8	244	3.20	8.01	2955.0	7.4638	734.21
9	226	2.69	111.95	2920.3	7.4747	696.26
10	128	0.92	2.35	2733.5	7.5482	487.54
11	128	0.92	2.35	2733.5	7.5482	487.54
12	128	0.92	4.70	2733.5	7.5482	487.54
13	65	0.25	53.63	2610.4	7.8094	286.61
14	65	0.25	53.63	2610.4	7.8094	286.61
15	65	0.25	107.25	2610.4	7.8094	286.61

* O. P. = Operating Point (according to Fig. 1 and Fig. 2)

** At the base ambient state

5. Results and discussion

Developed mechanical power in each segment of the analyzed steam turbine, which is one of the essential parameters in the exergy analysis of any turbine segment, is presented in Fig. 3.

Observing all segments, it can easily be concluded that the highest mechanical power of 47389.66 kW is developed in the first segment (Seg. I, which actually represents the entire HPC). Each of the first three IPC segments (Seg. II, III and IV) develop notable mechanical power, while the last IPC segment (Seg. V) develop the lowest mechanical power of all turbine segments, Fig. 3. Low pressure steam turbine cylinder is symmetrical one, therefore both LPC parts (LPC-L and LPC-R) develop the same mechanical power. Symmetrical LPC segments (Seg. VI and VIII, as well as Seg. VII and IX) also develop identical mechanical power.

In three cylinder steam turbines, LPC is usually the dominant mechanical power producer [13]. Steam turbine analyzed in this paper strongly deviates from this general guideline. In the observed steam turbine, IPC is the dominant mechanical power producer of all cylinders and it develops 48.95% of cumulative mechanical power produced in the whole turbine. HPC develops 29.68%, while

LPC (both LPC parts) develops only 21.37% of cumulative mechanical power produced in the whole turbine.

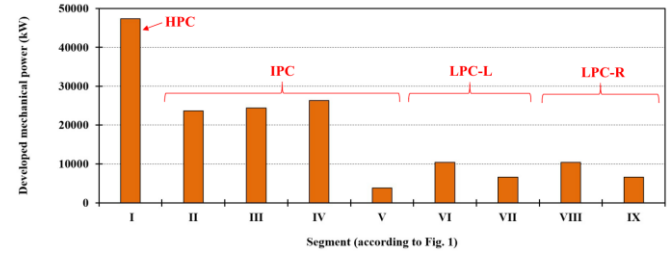


Fig. 3. Developed mechanical power in each segment of the analyzed steam turbine

Exergy destruction and exergy efficiency of each steam turbine segment at the base ambient state are presented in Fig. 4.

The outlet LPC segments (Seg. VII and IX) have the highest exergy destructions and the lowest exergy efficiency (equal to 61.27%) of all turbine segments. At least some turbine stages in the LPC outlet segments operate with wet steam (water droplets in wet steam increase losses) and in supersonic regime, so high exergy destruction and low exergy efficiency of the outlet LPC segments can be expected. The best exergy performance shows IPC segments – Seg. V has the lowest exergy destruction (equal to 363.84 kW), while Seg. II has the highest exergy efficiency (equal to 94.04%) of all turbine segments. HPC (Seg. I) has high exergy destruction, just slightly lower than outlet LPC segments, however such high exergy destruction did not notably ruin its exergy efficiency (Seg. I exergy efficiency is equal to 92.81%).

Considering turbine cylinders, in three cylinder steam turbines IPC usually shows the best performance (because IPC did not operate with the steam of the highest pressure in the plant as HPC and neither one IPC stage did not operate with wet steam as occur in some LPC outlet stages) [4, 13]. For the observed steam turbine, it can be concluded that HPC and IPC shows very similar exergy performance, so clear dominance of the IPC in relation to other cylinders did not exist for steam turbine analyzed in this paper.

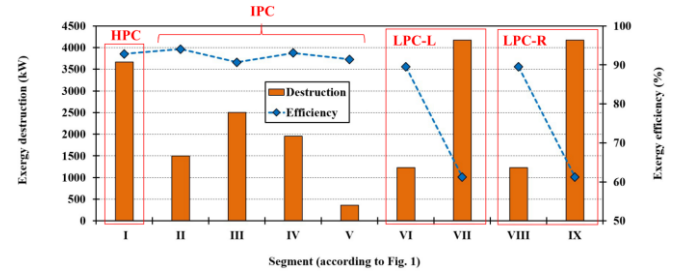


Fig. 4. Exergy destruction and exergy efficiency of each analyzed steam turbine segment at the base ambient state

Final part of the observed steam turbine segments research was related to each segment sensitivity on the ambient temperature change. During the ambient temperature variation, the ambient pressure remains always the same and identical as at the base ambient state. For each segment is valid a conclusion that increase in the ambient temperature simultaneously increases segment exergy destruction and decrease segment exergy efficiency. However, the intensity of exergy destruction and exergy efficiency change during the ambient temperature variation differs from one turbine segment to another.

Decrease in exergy efficiency of each steam turbine segment during the ambient temperature increase from 5 °C to 45 °C is presented in Fig. 5. From Fig. 5 is clear that LPC is much more sensitive to the ambient temperature change in comparison to other two cylinders. Outlet LPC segments (Seg. VII and IX) are the most sensitive to the ambient temperature change – their exergy efficiency decreases for 3.19% when the ambient temperature increases from 5 °C to 45 °C. Seg. II of the IPC is the least sensitive to the ambient temperature change – increase in the ambient temperature from 5 °C to 45 °C decreases Seg. II exergy efficiency for 0.71% only.

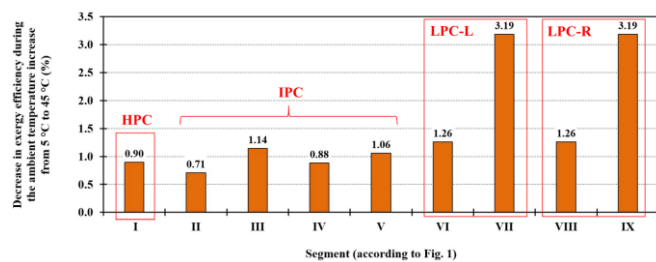


Fig. 5. Decrease in exergy efficiency of each steam turbine segment during the ambient temperature increase from 5 °C to 45 °C

6. Conclusions

Exergy analysis of complex three cylinder steam turbine segments is performed in this paper. The most important conclusions are:

- The highest mechanical power of 47389.66 kW is developed in the first segment (Seg. I, which actually represents the entire HPC).
- In the observed steam turbine, IPC is the dominant mechanical power producer of all cylinders and it develops 48.95% of cumulative mechanical power produced in the whole turbine. HPC develops 29.68%, while LPC (both LPC parts) develops only 21.37% of cumulative mechanical power produced in the whole turbine.
- The outlet LPC segments (Seg. VII and IX) have the highest exergy destructions and the lowest exergy efficiency (equal to 61.27%) of all turbine segments.
- The best exergy performance shows IPC segments – Seg. V has the lowest exergy destruction (equal to 363.84 kW), while Seg. II has the highest exergy efficiency (equal to 94.04%) of all turbine segments.
- HPC (Seg. I) has high exergy destruction, just slightly lower than outlet LPC segments, but sufficiently high exergy efficiency (Seg. I exergy efficiency is equal to 92.81%).
- LPC is much more sensitive to the ambient temperature change in comparison to other two cylinders (HPC and IPC).
- Outlet LPC segments (Seg. VII and IX) are the most sensitive to the ambient temperature change – their exergy efficiency decreases for 3.19% when the ambient temperature increases from 5 °C to 45 °C.

7. Acknowledgment

This research has been fully supported by the Croatian Science Foundation under the project IP-2022-10-2821.

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A Novel Hybrid Iterative Approach for Defining Plane Stress and Strain States

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Abstract: The definition of plane stress and strain states plays a crucial role in engineering, as recognizing these states within a structure enables the optimization of its design. This article introduces a novel hybrid wave method for analysing a circular disc geometry, with potential extensions to other geometric configurations. The method involves applying wave forces corresponding to the external loads, which then propagate through the structure and reflect back at the contours to satisfy static equilibrium conditions. The process is conceptualized in two phases while the first phase involves the application of forces where the second phase accounts for the arrival and reflection of waves at the contour boundaries. In each case, the static equilibrium at the contours is maintained by considering the reflection of the stress waves, or return waves. By incorporating these return waves and ensuring the boundary conditions are met, this approach facilitates the determination of stress at any given point within the structure.

Keywords: Stress wave distribution, plane stress, hybrid method, FEM

1. Introduction

The theory of elasticity problems in polar coordinates [1-8], specifically for stress and strain states, forms the foundation for the wave method presented in this study. These problems, derived from load configurations illustrated in Figure 1 [2, 9-15], are further expanded to special cases involving half-plane and plane geometries, as shown in Figures 2 and 3, where concentrated forces act. These configurations serve as the basis for developing the wave method used to analyse the disc subjected to both central and peripheral forces, akin to the scenario of the Brazilian Disc test [3, 4]. The forces in this context are self-equilibrated forces [5, 16-24], ensuring the balance of the applied loads.

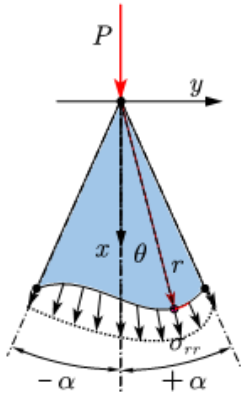


Fig. 1 Hypothetical schemes where forces act on wedge, case 1a force on wedge

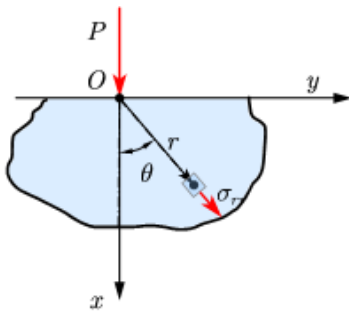


Fig. 2 Hypothetical schemes, case 1b force on half-plane

These schemes are basis point where we need to define stress distribution on circular disc or other geometry line. The distribution stress function is done according to Airy function [1, 6], which is a bi-harmonic function [2, 7]. The stresses for this function are shown in equation 1

$$\phi = A \cdot r \cdot \theta \cdot \sin(\theta) \quad (1)$$

$$\begin{pmatrix} \sigma_{rr} \\ \sigma_{\phi\phi} \\ \sigma_{r\phi} \end{pmatrix} = \begin{pmatrix} \frac{2 \cdot A}{r} \cdot \cos(\theta) \\ 0 \\ 0 \end{pmatrix}$$

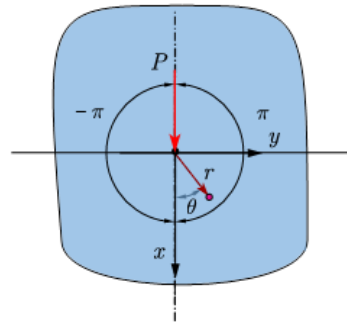


Fig. 3 Hypothetical schemes, case 1c force in plane (massive object)

Additionally, for the three cases shown in figures 1 to 3, coefficient A are:

$$\begin{aligned} A &= -\frac{P}{\alpha + \frac{\sin(2\alpha)}{2}} \rightarrow \text{case 1a} \\ A &= -\frac{2 \cdot P}{\pi} \rightarrow \text{case 1b} \\ A &= -\frac{P}{\pi} \rightarrow \text{case 1c} \end{aligned} \quad (2)$$

Once the coefficient A is determined, it can be substituted into equation 1 to define the stress state for the three cases, including the specific study case. For this study, we will focus on a disc with a radius $r = 25$ mm and thickness $t = 1$ mm. Unlike the problems analysed by other authors, the applied forces in our case are in centre, with an opposing or self-equilibrated force applied at the contour [27-33]. The stress distribution will follow the same approach as the Flamant problem [3, 8], as described in Equation 1, assuming pure radial stress. For the return waves or reflected waves, we will utilize the form of the group function [1], represented by bi-harmonic functions as shown in Table 1.

Table 1: Bi-harmonic functions for return waves.

	Sine functions	Cosine functions
Bi-harmonic functions	$\phi(x, y) = f(r) \cdot \sin(\theta)$	$\phi(x, y) = f(r) \cdot \cos(\theta)$
Case 1	$f(r) = A \cdot r^3$ $\begin{pmatrix} \sigma_{rr} \\ \sigma_{\phi\phi} \\ \sigma_{r\phi} \end{pmatrix} = \begin{pmatrix} 2 \cdot A \cdot r \cdot \sin(\theta) \\ 6 \cdot A \cdot r \cdot \sin(\theta) \\ -2 \cdot A \cdot r \cdot \cos(\theta) \end{pmatrix}$	$f(r) = A \cdot r^3$ $\begin{pmatrix} \sigma_{rr} \\ \sigma_{\phi\phi} \\ \sigma_{r\phi} \end{pmatrix} = \begin{pmatrix} 2 \cdot A \cdot r \cdot \cos(\theta) \\ 6 \cdot A \cdot r \cdot \cos(\theta) \\ 2 \cdot A \cdot r \cdot \sin(\theta) \end{pmatrix}$
Case 2	$f(r) = B \cdot \frac{1}{r}$	$f(r) = B \cdot \frac{1}{r}$

	$\begin{pmatrix} \sigma_{rr} \\ \sigma_{\varphi\varphi} \\ \sigma_{r\varphi} \end{pmatrix} = \begin{pmatrix} \frac{2 \cdot B}{r^3} \cdot \sin^2(\theta) \\ \frac{2 \cdot B}{r^3} \cdot \sin^2(\theta) \\ \frac{2 \cdot B}{r^3} \cdot \cos^2(\theta) \end{pmatrix}$	$\begin{pmatrix} \sigma_{rr} \\ \sigma_{\varphi\varphi} \\ \sigma_{r\varphi} \end{pmatrix} = \begin{pmatrix} \frac{2 \cdot B}{r^3} \cdot \cos^2(\theta) \\ \frac{2 \cdot B}{r^3} \cdot \cos^2(\theta) \\ -\frac{2 \cdot B}{r^3} \cdot \sin^2(\theta) \end{pmatrix}$
Case 3	$f(r) = C \cdot r \cdot \log \frac{r}{r_0}$ $\begin{pmatrix} \sigma_{rr} \\ \sigma_{\varphi\varphi} \\ \sigma_{r\varphi} \end{pmatrix} = \begin{pmatrix} \frac{C \cdot \sin^2(\theta)}{r} \\ \frac{C \cdot \sin^2(\theta)}{r} \\ -\frac{C \cdot \cos^2(\theta)}{r} \end{pmatrix}$	$f(r) = C \cdot r \cdot \log r$ $\begin{pmatrix} \sigma_{rr} \\ \sigma_{\varphi\varphi} \\ \sigma_{r\varphi} \end{pmatrix} = \begin{pmatrix} \frac{C \cdot \cos^2(\theta)}{r} \\ \frac{C \cdot \cos^2(\theta)}{r} \\ \frac{C \cdot \sin^2(\theta)}{r} \end{pmatrix}$

This approach continues iteratively with the propagation of waves and return waves until equilibrium is reached at the contour. Once the equilibrium states at the boundary are satisfied, the normal and tangential stress components at all points within the disc can be determined. These stresses are derived based on the wave propagation and distribution states. This iterative process forms the theoretical foundation for the new hybrid method, ensuring that the boundary conditions are met and the system stabilizes. The method allows for a comprehensive stress analysis across the entire disc, based on the dynamic interaction of the waves and their reflections.

2. Hybrid waves method for stress distribution

We will take a central force and peripheral force as the equivalent force to zero, as shown in figure 4. Central forces here are force in plane, as case 1c shown in figure 3. Radial stress, from these forces, are:

$$\sigma_{r1} = \frac{-2 \cdot P}{\pi \cdot r_1} \cdot \cos^2(\theta_1) \quad (3)$$

$$\sigma_{r2} = \frac{-P}{\pi \cdot r_2} \cdot \cos^2(\theta_2) \quad (4)$$

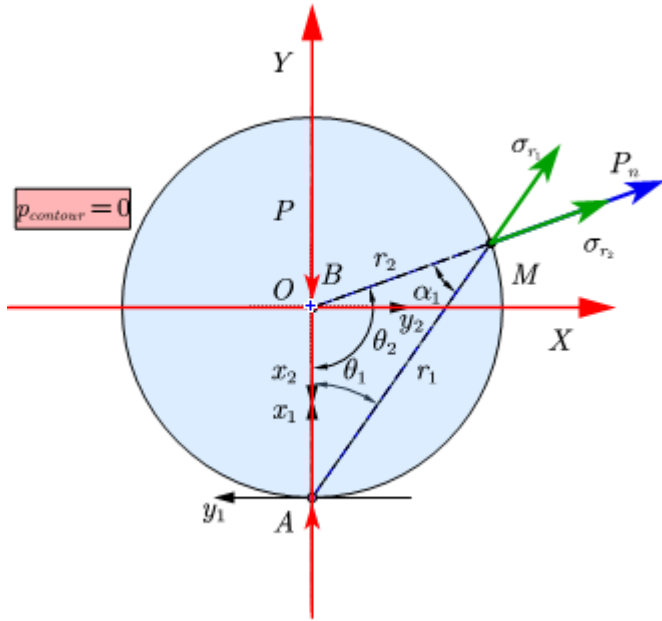


Fig. 4 Self-equilibrated forces, central and contour forces.

From the triangle ABM we have: $r_1 = D \cdot \cos \theta_1$ and $r_2 = D/2$ so the equations 3 and 4 will transform:

$$\sigma_{r1} = \frac{-2 \cdot P}{\pi \cdot D} \quad (5)$$

$$\sigma_{r2} = \frac{-2 \cdot P}{\pi \cdot D} \cdot \cos^2(\theta_2) \quad (6)$$

Radial pressure for 2 waves will be:

$$\begin{pmatrix} pr \\ p\theta \end{pmatrix} = \begin{pmatrix} \sigma_{rr} & 0 \\ 0 & 0 \end{pmatrix} \begin{pmatrix} \cos \alpha \\ \sin \alpha \end{pmatrix}$$

and $\alpha_1 = \theta_1$, $\alpha_2 = 0$

$$pr_1 = \sigma_{r1} \cdot \cos \alpha_1 = \frac{-2 \cdot P}{\pi \cdot D} \cdot \cos \theta_1$$

$$pr_2 = \sigma_{r2} = \frac{-2 \cdot P}{\pi \cdot D} \cdot \cos^2(\theta_2)$$

Transforming equation 5 and 6, we will get normal and tangential pressure in contour as:

$$p_N = pr_1 \cdot \cos \alpha_1 + pr_2 = \frac{-2 \cdot P}{\pi \cdot D} \cdot \cos^2 \theta_1 + \frac{-2 \cdot P}{\pi \cdot D} \cdot \cos^2(\theta_2) \quad (7)$$

$$p_T = pr_1 \cdot \sin \alpha_1 = \frac{-P}{\pi \cdot D} \cdot \sin(2 \cdot \theta_1) \quad (8)$$

From geometry we have the angle definition: $2 \cdot \theta_1 + \theta_2 = \pi$, and replacing in equation 7 and 8 the equation of these pressure distribution will be as follows:

$$p_N = \frac{-2 \cdot P}{\pi \cdot D} \cdot (\cos^2 \theta_1 + \cos^2(\theta_2)) = \frac{-2 \cdot P}{\pi \cdot D} \cdot \left(\frac{1 + \cos(2 \cdot \theta_1)}{2} + \cos^2(\theta_2) \right)$$

$$p_N = \frac{-2 \cdot P}{\pi \cdot D} \cdot \left(\frac{1 - \cos(\theta_2)}{2} + \cos^2(\theta_2) \right) = \frac{-P}{\pi \cdot D} + \frac{-P}{\pi \cdot D} \cdot \cos(\theta_2)$$

$$p_T = \frac{-P}{\pi \cdot D} \cdot \sin(\theta_2)$$

Once the distribution equations for the normal and tangential pressures are derived, we can plot these equations graphically. In the resulting graphs, the x-axis will represent the perimeter of the "catted" circle, while the y-axis will depict the corresponding normal and tangential pressures. These plots will be shown in Figures 5, 6, and 7, providing a visual representation of the pressure distribution across the disc's perimeter.

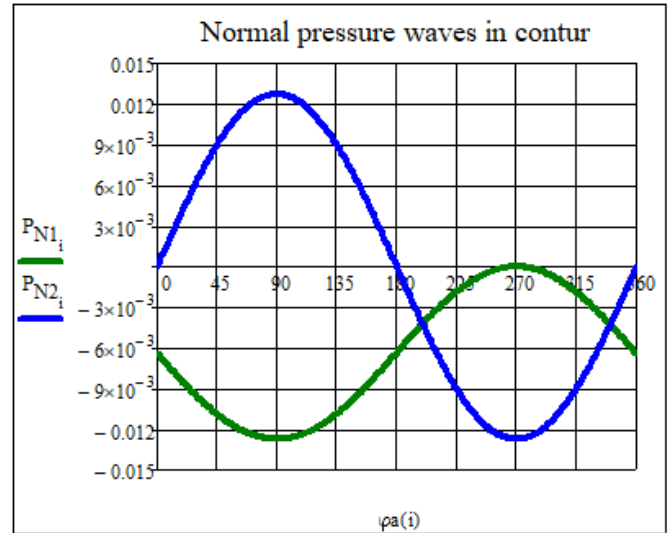


Fig. 5 Normal pressure waves in contour from 2 forces.

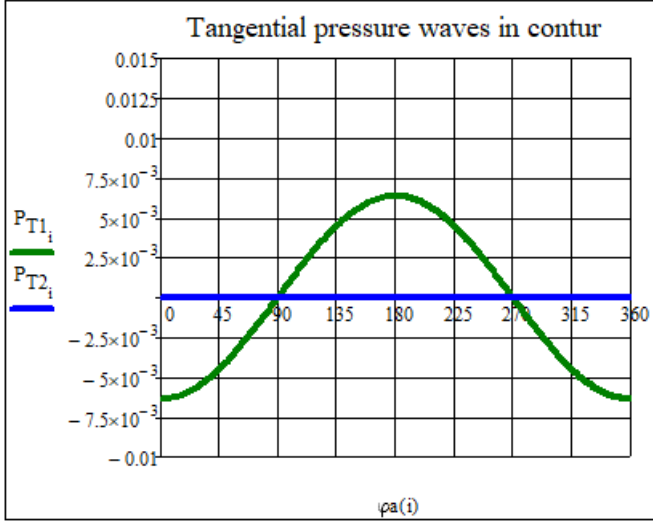


Fig. 6 Tangential pressure waves in contour from 2 forces. .

The normal and tangential pressure waves will be distributed along the contour. Given that the pressure at the contour must be zero (as per the boundary condition), return waves are introduced to satisfy this requirement. The solution can be approached in two ways: either step-by-step, where each wave is addressed individually, or by allowing all waves to propagate to the contour and then adding the negative sum of the return waves to fulfil the contour condition. The return waves, or reflected waves, can be determined using the functions presented in Table 1. These functions will guide the formulation of the return waves, ensuring that the static equilibrium condition at the contour is met.

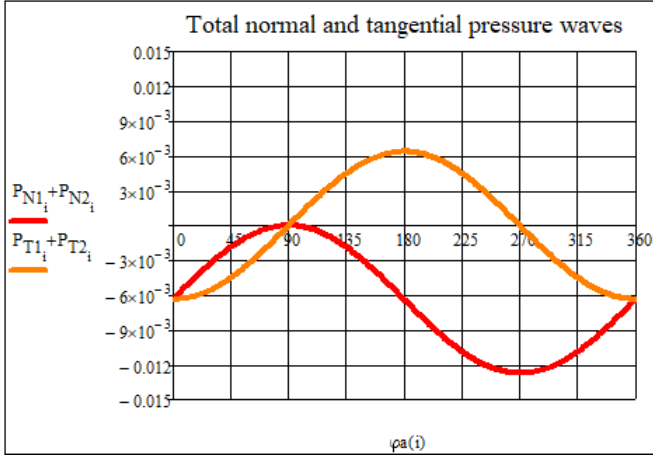


Fig. 7 Normal pressure waves in contour from 2 forces.

3. Return-waves analysis

Mathematical solution of the return waves, taken from cases discussed in table 1, can be expressed in equation 9 and 10.

$$p_N = \frac{-P}{\pi \cdot D} + \frac{-P}{\pi \cdot D} \cdot \cos(\theta_2) + f_{wave\ 1n} + f_{wave\ 2n} = 0 \quad (9)$$

$$p_T = \frac{-P}{\pi \cdot D} \cdot \sin(\theta_2) + f_{wave\ 2t} = 0 \quad (10)$$

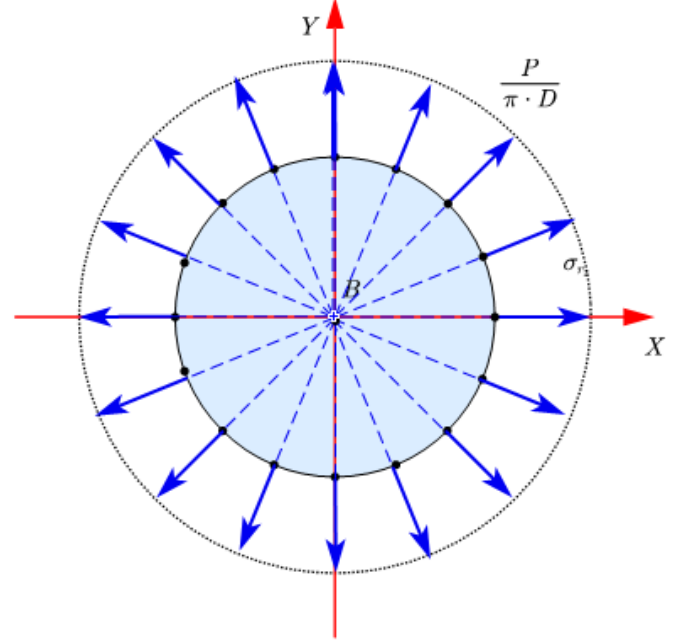


Fig. 8 Constant pressure waves distribution in contour.

First return-waves function for normal stress will be $f_{wave1n} = \frac{P}{\pi \cdot D}$, and in cartesian coordinates Airy function is $\Phi = \frac{A \cdot x^2}{2} + \frac{A \cdot y^2}{2}$. So, the normal and tangential stress in any point of disc are:

$$\sigma_{xx} = A, \sigma_{yy} = A, \sigma_{xy} = 0$$

From the static condition in contour, we have:

$$\begin{pmatrix} \sigma_{xx} & \sigma_{xy} \\ \sigma_{xy} & \sigma_{yy} \end{pmatrix} \begin{pmatrix} l \\ m \end{pmatrix} = \frac{P}{\pi \cdot D} \begin{pmatrix} l \\ m \end{pmatrix} \rightarrow A = \frac{P}{\pi \cdot D} \quad (11)$$

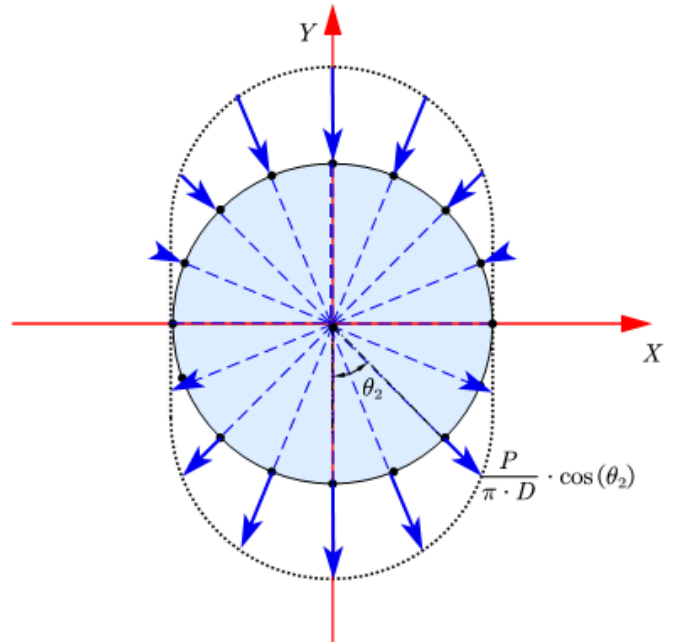


Fig. 9 Normal pressure waves distribution in contour

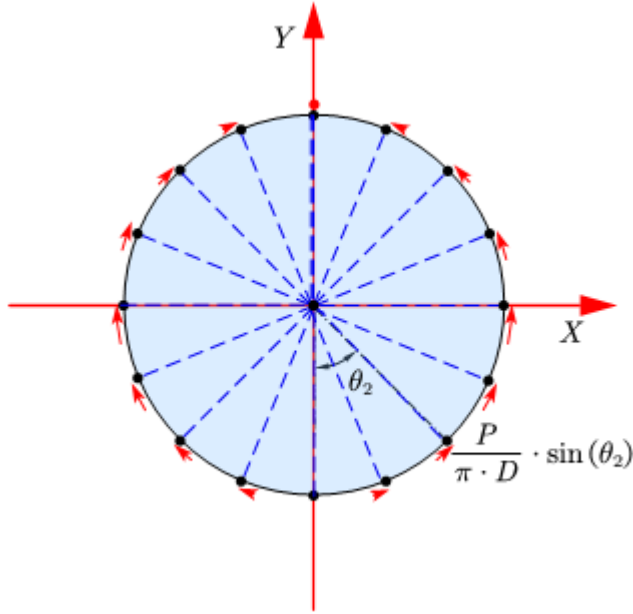


Fig. 10 Tangential waves distribution in contour

The second return waves function for normal waves: $f_{wave2n} = \frac{P}{\pi \cdot D} \cdot \cos(\theta_2)$ and tangential $f_{wave2t} = \frac{P}{\pi \cdot D} \cdot \sin(\theta_2)$. In polar coordinates the bi-harmonic pressure will be: $\Phi = C \cdot r \cdot \log(r) \cdot \cos \theta_2$ where stresses are:

$$\begin{pmatrix} \sigma_{r3} \\ \sigma_{\phi 3} \\ \tau_{r3} \end{pmatrix} = \begin{pmatrix} \frac{C \cdot \cos(\theta_2)}{r} \\ \frac{C \cdot \cos(\theta_2)}{r} \\ \frac{C \cdot \sin(\theta_2)}{r} \end{pmatrix} \quad (12)$$

From the static condition in contour, we have:

$$\begin{pmatrix} \sigma_{r3} & \tau_{r3} \\ \tau_{r3} & \sigma_{\phi 3} \end{pmatrix} \begin{pmatrix} 1 \\ 0 \end{pmatrix} = \frac{P}{\pi \cdot D} \begin{pmatrix} \cos(\theta_2) \\ \sin(\theta_2) \end{pmatrix} \rightarrow C = \frac{P}{2 \cdot \pi} \quad (13)$$

After the recent waves were given, both normal and tangential, we can say that the method was completed, completing the conditions in the contours taken from equations 6 and 7, and shown in figures 8, 9 and 10.

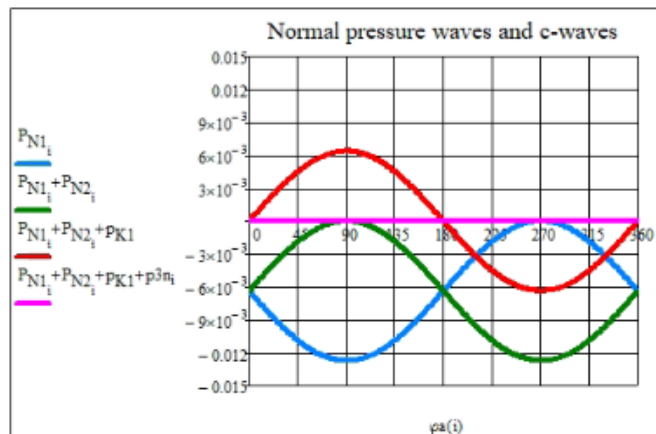


Fig. 11 Progressive collection of all normal waves and return waves

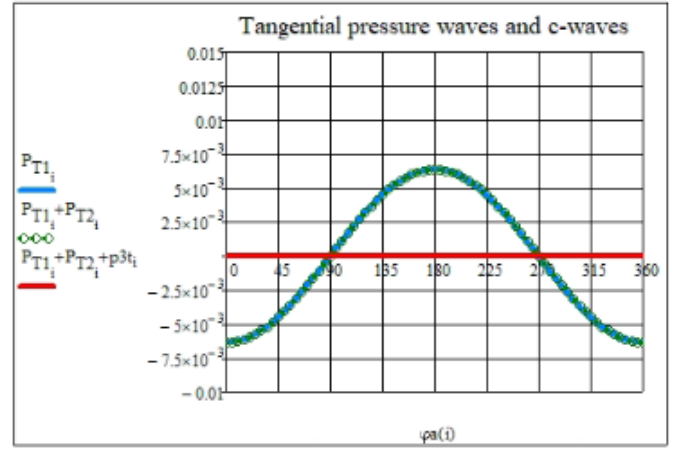


Fig. 12 Progressive collection of all tangential waves and return waves.

4. Determining the stress in internal points

Once the static equilibrium condition is satisfied at the contour, we can proceed to check the stress at every point within the disc. The stress at each point will be the sum of the stresses contributed by each wave, calculated along the corresponding coordinate axes. To ensure the accuracy and reliability of this method, we will employ the finite element method (FEM) as a verification tool. FEM will be used to calculate the normal and tangential stresses on both the vertical and horizontal planes, allowing for a comparison with the results obtained from the wave-based method. This will help validate the wave method and ensure that it provides consistent and accurate stress distributions across the disc.

Equation for this stress in vertical axes are shown in equation 14, 15 and 16.

$$\sigma_{xx} = \sigma_{r1} \cdot \sin^2(\theta_1) + \sigma_{r2} \cdot \sin^2(\theta_2) + \frac{P}{\pi \cdot D} + \sigma_{r3} \cdot \sin^2(\theta_2) + \sigma_{\phi 3} \cdot \cos^2(\theta_2) + \tau_{r3} \cdot \sin(2 \cdot \theta_2) \quad (14)$$

$$\sigma_{yy} = \sigma_{r1} \cdot \cos^2(\theta_1) + \sigma_{r2} \cdot \cos^2(\theta_2) + \frac{P}{\pi \cdot D} + \sigma_{r3} \cdot \cos^2(\theta_2) + \sigma_{\phi 3} \cdot \sin^2(\theta_2) - \tau_{r3} \cdot \sin(2 \cdot \theta_2) \quad (15)$$

$$\sigma_{xy} = 0.5 \cdot \sigma_{r1} \cdot \sin(2 \cdot \theta_1) + 0.5 \cdot \sigma_{r2} \cdot \sin(2 \cdot \theta_2) + 0.5 \cdot \sigma_{r3} \cdot \sin(2 \cdot \theta_2) + 0.5 \cdot \sigma_{\phi 3} \cdot \sin(2 \cdot \theta_2) - \tau_{r3} \cdot \cos(2 \cdot \theta_2) \quad (16)$$

The results of these stresses are shown in graphics in figures below.

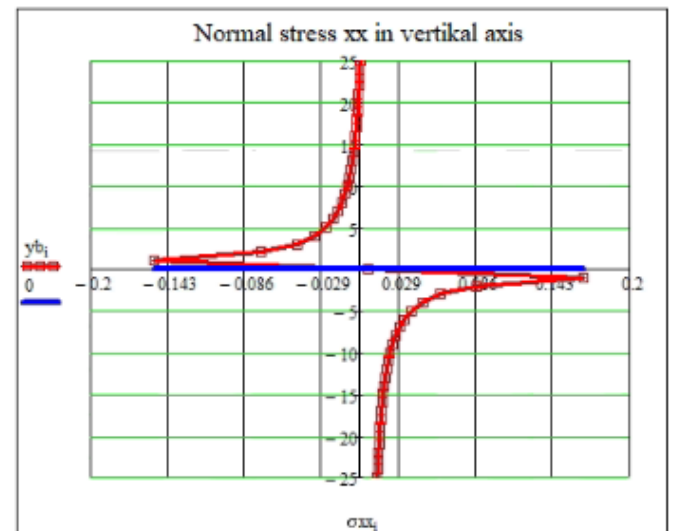


Fig. 13 Normal stress xx in vertical axis. .

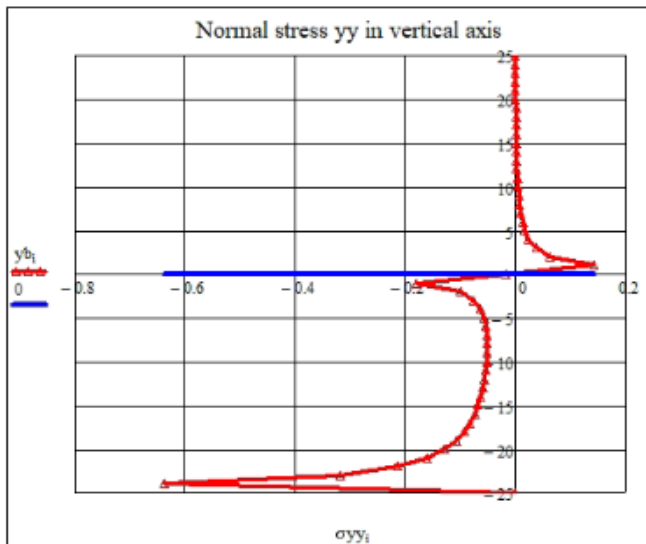


Fig. 14 Normal stress yy in vertical axis.

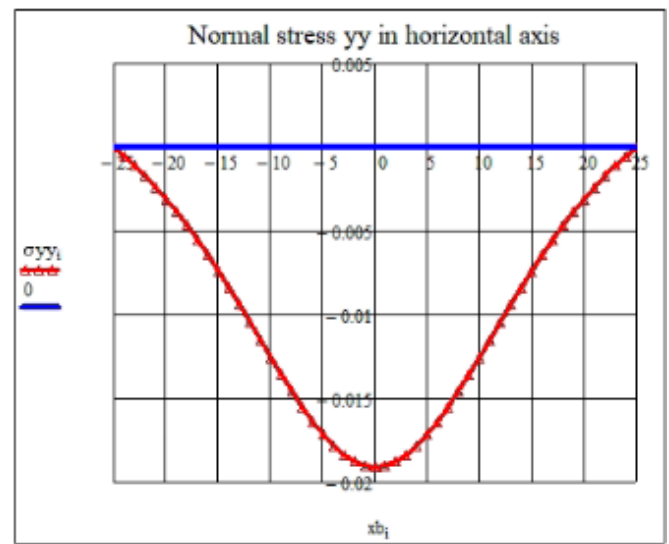


Fig. 17 Normal stress yy in horizontal axis.

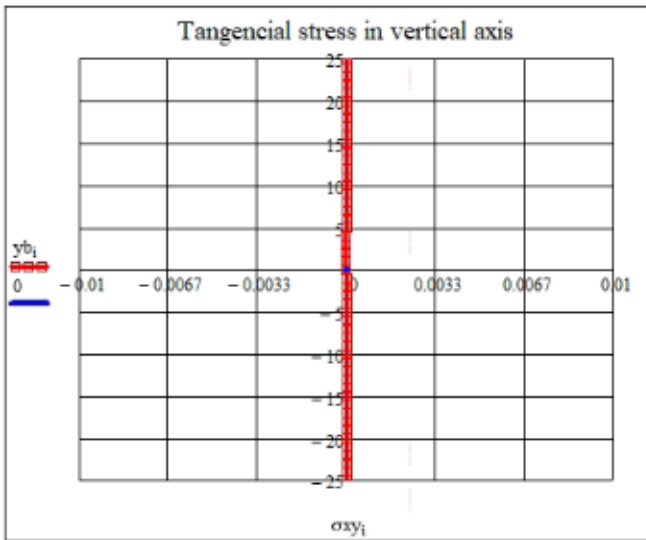


Fig. 15 Tangential stress in vertical axis.

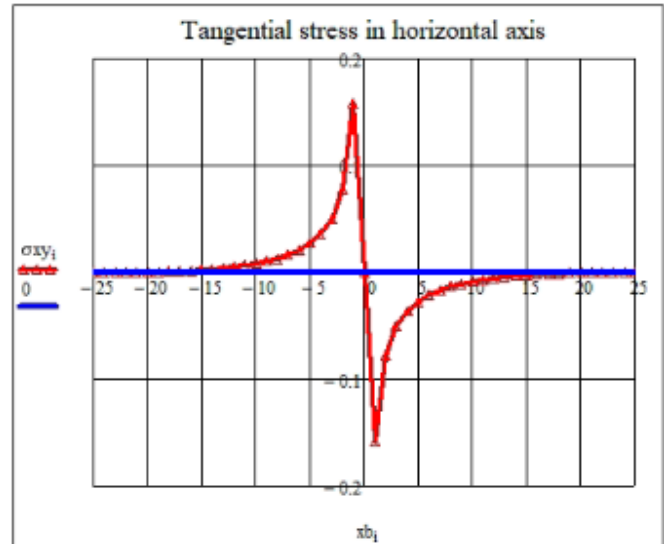


Fig. 18 Tangential stress in horizontal axis.

The same method will be followed for horizontal plan, and in this case the results will be:

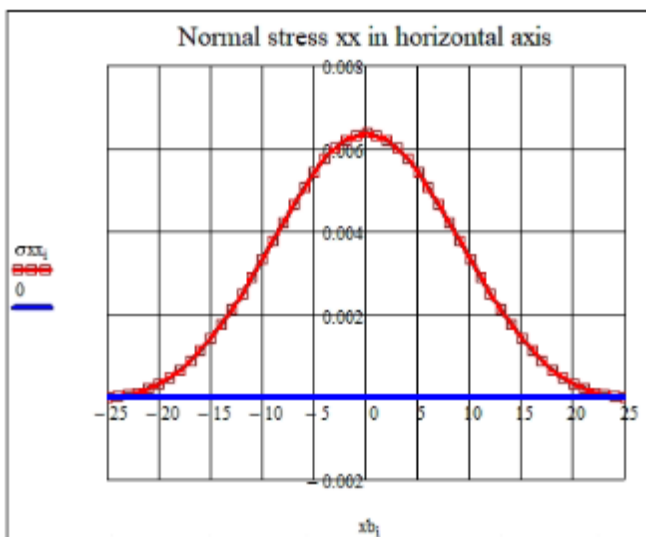
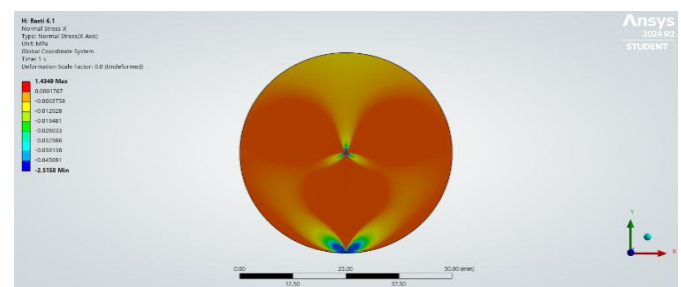


Fig. 16 Normal stress xx in horizontal axis.

5. Comparison results with the FEM

After obtaining the stress results from the wave-based method described earlier, we will compare these findings with those obtained through a virtual analysis using the finite element method (FEM) in ANSYS software. This comparison will be conducted for each plane, allowing us to assess the accuracy and consistency of the wave method in relation to the FEM results. For both the vertical and horizontal planes, the normal and tangential strain distributions will be plotted and shown in the following figures. These visual representations will help illustrate how the strain fields compare between the two methods, providing further validation of the proposed hybrid wave method.



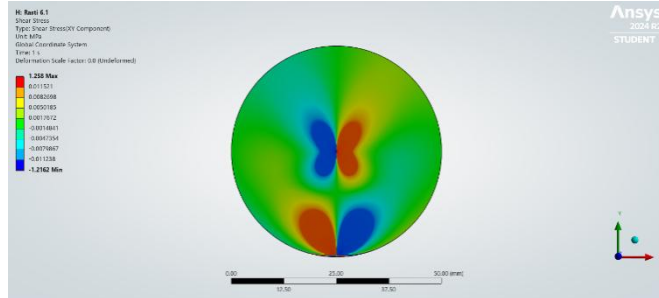
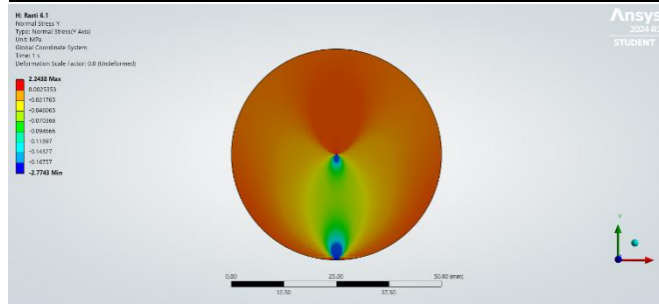


Fig. 19 Analysis disc with Ansys, for case Centre Forces – Contour Forces.

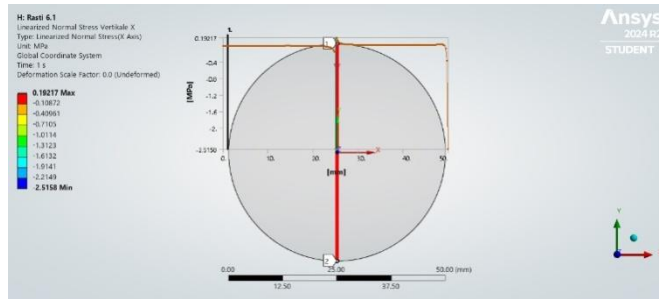


Fig. 20 Normal stress xx in vertical axis.

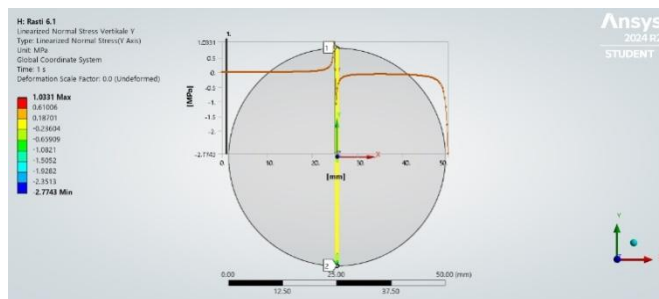


Fig. 21 Normal stress yy in vertical axis.

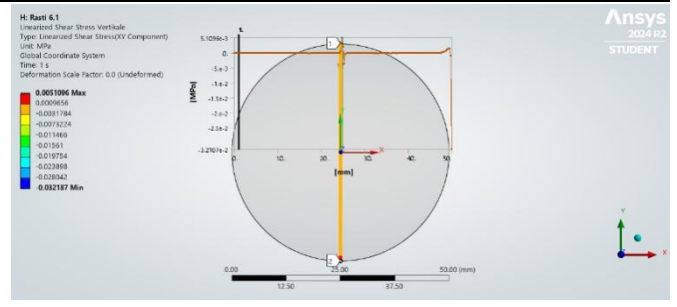


Fig. 22 Tangential stress in vertical axis.

• Horizontal axis

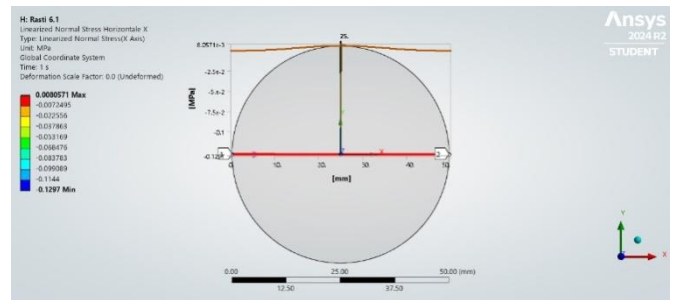


Fig. 23 Normal stress xx in horizontal axis.

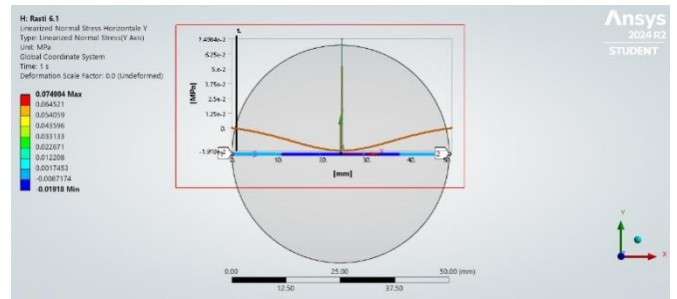


Fig. 24 Normal stress yy in horizontal axis.

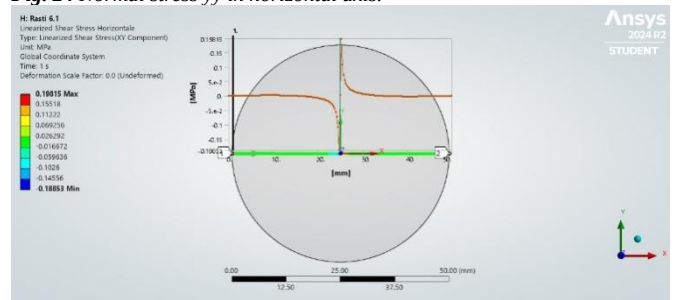


Fig. 25 Tangential stress in horizontal axis.

The results are clearly the same as that means that the method of splitting the load into waves gives us accurate results.

6. Summary and Conclusion

In this work, we have developed and demonstrated a wave-based method for determining the stress state in a study object, which can subsequently be used for various optimizations. This method leverages the principle of force independence, where forces act independently, and their effects are transmitted through the stress distribution derived from the Flamant problem or the corresponding laws of elasticity.

The hybrid wave method we propose involves the return of the applied load both normal and tangential pressures at the static contour. Although this process involves an abstraction, as the nature of the return waves differs, it effectively identifies these "anti-waves" from the field of biharmonic functions, as outlined in our results. These return waves are then reintroduced into the body of the disc, with each wave contributing to the stress at every point within the structure.

The results obtained from this hybrid wave method have been validated by comparisons with finite element method (FEM) analyses. The normal and tangential stress distributions on both the vertical and horizontal planes were found to be in excellent agreement with FEM results, demonstrating the accuracy and reliability of the proposed method.

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The influence of vibrations on the friction of removable electrical connectors during operation, and the causes of malfunctions in electrical contacts

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Abstract: This paper discusses contact interaction under mechanical stress and how vibrations change the physical properties of conductor materials, which affects their ability to provide reliable contact. It also analyzes the causes of contact failures, which allows us to better understand the mechanisms that lead to problems in the operation of electrical connectors. These factors can lead to serious consequences, including breakage and reduced reliability of connectors, and also considers the possibilities of improving the characteristics of electrical contacts at the stage of their creation, using various technologies for applying and creating an approximately ideal molecular structure of the contact surface of detachable electrical connectors, with an increase in the surface layer of the contact and a decrease in the coefficient of friction.

KEY WORDS: VIBRATION EFFECTS, TRIBOLOGY, TRIBOTECHNICS, ELECTRICAL CONTACTS, ELECTRICAL CONNECTORS.

Introduction

Modern electrical connectors play a key role in ensuring the reliability and efficiency of electrical and electronic systems, finding application in the automotive industry, aviation, telecommunications and consumer electronics. Detachable electrical connectors, in particular, allow for quick connection and disconnection of electrical circuits, which makes them indispensable in conditions of frequent replacement or maintenance of equipment. However, there are many factors that negatively affect their performance and durability, including vibrations that occur during operation. The relevance of this study is due to the fact that vibration effects can cause serious problems, such as deterioration of contact interaction, increased resistance, overheating and failure of connectors. Under conditions of constant mechanical impacts, it is important to understand how vibrations affect friction in detachable electrical connectors and what measures can be taken to minimize the associated risks. The article analyzes the effect of vibrations on the friction of connected contacts, identifies various types of vibrations and their sources that affect the quality of an electrical connection. Contact interaction under mechanical stress and how vibrations change the physical properties of conductor materials, which affects their ability to provide reliable contact, are considered. An analysis of the causes of contact failures was also carried out, which allows for a better understanding of the mechanisms that lead to problems in the operation of electrical connectors.

Results and discussion

Analysis of the Effect of Vibration on Friction of Connected Contacts. Vibrations that occur during the operation of electrical and electronic systems significantly affect the condition and reliability of removable electrical connectors. These mechanical vibrations can be caused by the operation of engines or external conditions, for example, in vehicles and industry. Understanding the effect of vibrations on friction in connected contacts is critical to ensuring their reliability and durability. Contact interaction in connectors is based on microscopic irregularities in the surfaces of the materials from which the electrodes are made. When connected, the contacts deform, increasing the contact area and reducing electrical resistance. However, vibrations can change the mechanical characteristics of connectors, causing microcracks on the contact surfaces, which leads to increased friction and changes in pressure distribution. At high vibration levels, a change in the contact structure occurs [1]. Vibrations can loosen connected elements, increasing wear and aging of materials, which makes the interaction between the contacts less predictable and can lead to a loss of sealing of the connection. Contaminants from mechanical impacts also reduce conductivity, and their accumulation under the influence of vibration aggravates the situation, creating difficulties in establishing stable contact. The durability of connectors can be affected by resonance phenomena that occur at certain vibration frequencies. If the vibration frequency coincides with the natural frequencies of the system, this can lead to significant vibrations in the connectors, causing additional energy that contributes to the formation of microscopic cracks and other damage.

Types of vibrations and their sources. Vibrations caused by mechanical influences can be classified into several types, each with its own sources and level of impact on electrical connectors. Understanding these types of vibrations, their amplitude, frequency and nature of impact is important for assessing the behavior and durability of connections in electrical systems. The first type is periodic vibrations caused by the operation of rotating machines such as generators, motors and compressors[2]. These vibrations have a fixed frequency corresponding to the rotation speed of mechanical units and can be transmitted through the structures on which the devices are installed. Periodic vibrations can cause resonance effects that amplify the vibrations and increase the loads on electrical connectors. The second type is random vibrations caused by various external factors such as impacts, jolts or atmospheric phenomena such as strong wind. These vibrations are characterized by amplitude and frequency that vary over time, making them unstable and difficult to predict. This increases the likelihood of damage to electrical contacts due to random loads. The third type is impact vibrations, caused by sudden dynamic impacts that are usually short in duration but high in intensity. Examples include equipment falling or sudden stops and starts of machines. Impact vibrations can cause instantaneous deformations in conductor and connector materials, which can lead to serious damage and reduced reliability of connections.

Contact interaction under mechanical stress. When using pluggable electrical connectors, it is important to consider the mechanical effects of vibrations on contacts, which may be caused by motors, adjacent machinery, vehicles or natural phenomena. These vibrations can degrade the interaction between contacts and reduce the reliability of the connections. Vibrations affect the mechanical properties of materials, causing temporary changes in the shape and parameters of contacts. Depending on the structure of the materials, this can lead to compaction or wear, which reduces the pressure on the contact surfaces and, therefore, the effectiveness of the connection [3]. Cyclic loads can also cause material fatigue and the formation of microcracks, which degrade the quality of contact and create risks for the reliability of the connection. These changes can affect both the surface and the internal layers of the material. In addition, vibrations can displace contacts, changing their position and reducing the effectiveness of the connection. Insufficient contact can lead to intermittent current, overheating and deterioration of the electrical properties. It is important to consider all these factors to ensure the reliability of electrical connectors.

Physical properties of conductor materials. Conductor materials have unique physical properties that affect their behavior under mechanical influences such as vibration. Key characteristics are electrical conductivity, mechanical strength, and corrosion resistance, which are important for understanding the effects of vibration loads on electrical connectors. The electrical conductivity of copper and aluminum depends on their atomic structure and free electron density. Vibrations can cause oscillations that lead to shifts in the crystal lattice and deterioration of contact characteristics. At high frequencies, fatigue cracks are possible, which reduces the reliability of connections [4]. Mechanical strength is also critical,

since vibration loads can cause deformations and accumulation of fatigue damage, which leads to a decrease in the strength of connectors and their failure. Low strength can contribute to micropressures and open pathways for corrosion. Corrosion resistance is important for the durability of connections, especially in aggressive environments where vibrations can aggravate corrosion processes. Taking all these factors into account is necessary to ensure the reliability of electrical connectors under mechanical influences.

Contact Failure Root Analysis . Vibration-induced electrical contact failures are a complex issue affecting the design, operation, and monitoring of removable electrical connectors. Understanding the causes of such failures requires consideration of many factors, including the mechanical properties of materials, the nature of the contact interaction, and the types of vibration. One of the main causes of failures is insufficient mechanical strength of connector materials. Vibration loads can cause uneven stress distribution, leading to fatigue failure. Different vibration frequencies can cause different vibration amplitudes, which changes the properties of materials and can lead to cracks or wear if permissible stress limits are exceeded [5]. The nature of the contact interaction between conductors is also important. Parameters such as the amount of pressure, the geometry of the contact surfaces, and the presence of contaminants affect the possibility of failure. Under constant vibration conditions, the contact may oscillate, which changes its quality. Incorrect angle of inclination, insufficient rigidity of the structure or inadequate clamping can lead to divergence of connections, increased resistance and overheating of contacts. The condition of the contact surfaces is also critical. Oxide and other coatings can form on electrical contacts, affecting their characteristics. Mechanical loads during vibration can cause a flickering contact, which hinders the transmission of electric current and leads to additional losses.

Methods for Minimizing Operational Risks . Effective risk management for removable electrical connectors requires an integrated approach to minimizing the impact of vibration on friction and contact reliability. It is important to consider many factors during the design, installation, and operation stages. The first step in reducing risks is careful selection of materials. Using conductors with high mechanical and electrical properties, such as alloys with high compressive strength and wear resistance, can reduce the likelihood of failure. The friction coefficients of different materials should also be taken into account to optimize contacting [6]. The manufacturing processes of connectors are also important. Controlling tolerances and surface roughness of contacts during production can significantly affect the amount of friction. Laser processing and special coatings can improve material compatibility and increase the service life of connectors. Also, the use of galvanic methods of applying and processing electrical contacts is usually the most effective and durable, with the least likelihood of failure of contact connectors along with other methods of processing and creating electrical contacts. Installation and operation must take into account possible mechanical effects. The location of connectors, vibration of surrounding equipment, and the level of attachment are critical. Engineers must design wiring diagrams that ensure secure attachment and reduce connector movement. Dynamic testing to standards can help identify vulnerabilities at the design stage. Regular maintenance also plays an important role in preventing failures. Connector condition checks can help identify and resolve problems before they occur.

Prospects for Research in Connector Reliability . Prospects for research in connector reliability open up new opportunities for improving technologies and increasing the efficiency of electrical systems. The relevance of the topic is due to the growing requirements for the reliability and durability of connectors under harsh operating conditions, where vibrations and mechanical loads can significantly affect their performance. One of the areas of research is an in-depth understanding of the processes occurring in contacts under the influence of vibrations. Current research shows that vibrations cause dynamic changes in contact zones, which affects normal and friction contact forces. To create

more accurate models predicting the behavior of connectors under various conditions, it is necessary to combine theoretical and experimental approaches [7]. Studying the interaction of materials from which electrical connectors are made is also important for improving reliability. The development of new alloys with improved physical and chemical properties can reduce the likelihood of oxidation and destruction of contact surfaces. The use of modern technologies, galvanic treatment of special coatings, allows us to create connectors that are more resistant to mechanical wear and vibration. Implementing active monitoring of the condition of connections during operation can significantly improve their behavior. Using sensors to monitor vibration and temperature allows early detection of potential faults and the implementation of the concept of predictive maintenance, which increases reliability and reduces replacement costs. Attention should also be paid to the study of model figures on which the mechanical characteristics of connectors are based. Modern numerical modeling methods can help in this process, providing a deeper understanding of the behavior of connectors under various operating conditions.

Conclusion

The study found that vibration effects from the operation of various mechanisms and equipment significantly affect the contact interaction in electrical connectors. These effects can change the friction coefficient, which worsens the quality of electrical contact and can lead to malfunctions. Vibrations can be both periodic and random, with varying intensity depending on the operating conditions. Contact interaction under mechanical stress was also analyzed in detail. The initial contact pressure and the mass of the moving parts play a key role in the reliability of connectors. Insufficient pressure can cause microcracks and increase contact resistance, which leads to overheating and other problems. Uneven achievement of the working pressure can negatively affect the durability of connectors, causing uneven load distribution and deterioration in contact quality. The physical properties of conductor materials are also significant. Different materials have different mechanical and electrical properties, which affects their behavior under vibration loads. In the course of the work, the main causes of malfunctions in electrical contacts were identified, including overheating, insufficient pressure and abrupt achievement of the working pressure. These factors can lead to serious consequences, including breakdowns and reduced reliability of connectors, and the possibilities of improving the characteristics of electrical contacts at the stage of their creation are considered. In this direction, the most promising is the study of applying galvanic coatings to electrical connectors, using various technologies for applying and creating an approximately ideal molecular structure of the contact surface of removable electrical connectors, with an increase in the surface layer of the contact and a decrease in the friction coefficient.

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Mooring system for fuel oil unloading

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Abstract: The aim of this paper is the calculation of the PLEM (Pipeline End Manifold) System of the 18" dia. subsea pipeline, according to the International Codes and Standards. A Geotechnical survey was carried out to define soil – structure interaction, effect of scour, soil stratigraphy and susceptibility to liquefaction. Then were defined the acting forces on PLEM, pipeline and submarine hoses taking in consideration the total buoyancy as well. The calculation was carried out for filled pipeline (in service), as well as for empty pipeline. The ground conditions along the sealines and at the location of the PLEM have been investigated by a dedicated additional site investigation, consisting of 12 no. vibrocore samplings and the related laboratory testing. The stability of PLEM was checked for several load combination. This calculation it was very important for environmental reason and shows that the structure meet the requirements of the project.

KEY-WORDS: PLEM, SUBMARINE PIPELINE, TOTAL BUOYANCY, LIMIT EQUILIBRIUM

1. Introduction

A fuel oil (FO) line 3.5 km long and ND 18" was designed and installed offshore in Vlora Bay to transfer fuel from vessels to onshore FO tanks located near the Combined Cycle Power Plant of Vlora. The plant should operate with fuel oil D2, because the natural gas is not available in Albania. A Pipeline End Manifold (PLEM) was installed, and the Mooring system capacity was to accommodate 15 T oil tanker, the quantity needed for one month operation of the plant in base load.

Due to environment reasons the offshore structures and pipelines should be reliable, despite of the high price of the erection.

Albania has a short history in the field of offshore pipeline constructions. Consequently, this paper is a new approach towards finding satisfactory solutions.

Investigations consisted of site activities and laboratory tests and the results are reported here below. The data collected provide a strong tool to compare with geotechnical analysis of structures and to calibrate the used models.

The behavior of the structure was checked under lateral loads to verify its reliability [1]. Moreover, the analysis of stiffness and soil–structure interaction were carried out. The analysis that provides information on soil composition is necessary to assess the susceptibility to liquefaction of natural on-site shallow soils.

2. Design data

Pipeline [2]	
Manufacture process	SAW /ERW
Nominal Diameter	18" = 457.20 mm
Inner diameter	438.16
mm Inner section	1507 cm ²
Assumed Wall Thickness	0.375" = 9.52 mm
Material	API 5L — X52
SMYS	52000 psi = 358.6 Mpa = 3655 kg/cm ²
Max Working pressure	15 barg (including surge)
Flow rate (max)	1500 m ³ /h
Max water depth	– 12.00 m
Max operative pressure	15 barg

Fluid [3]	
Type	Fuel Oil
Density (40°C)	856 kg/m ³
Viscosity (40°C)	2.7 cSt at 38°C

Submarine hoses [4]	
No	2
Nominal Diameter	8" = 203.2 mm
Material	reinforced rubber / synthetic rubber
Total length	35.0 m

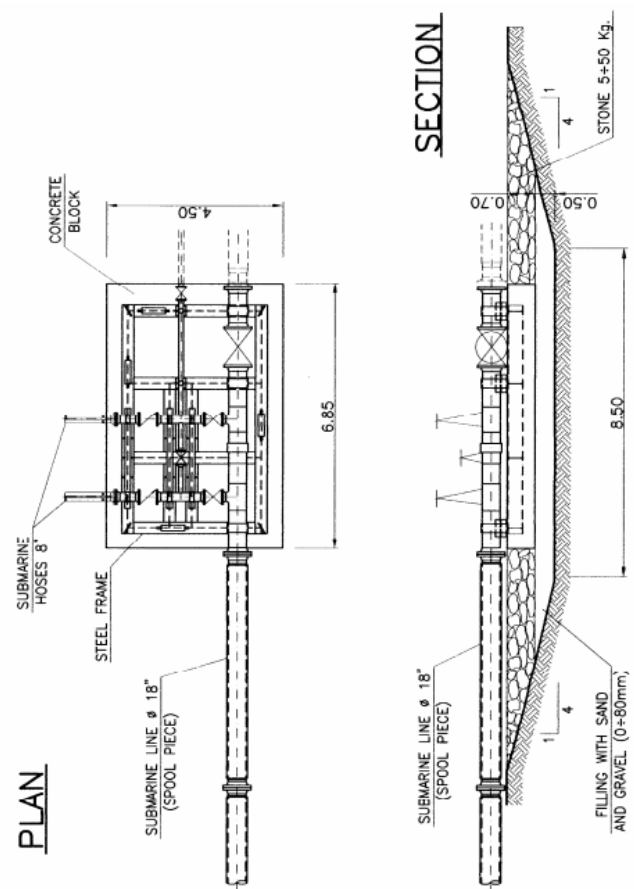


Figure 1. PLEM

3. Weights

With reference to the Figure 1, the following Weights and Buoyancy forces must be considered:

3.1 Weight in open air, empty pipe

Concrete block	6.85 m * 4.50 m * .70 m * 2.4 t/m ³	≈ 51.80 t
Steel frame		≈ 5.30 t
Piping dia. 18"		≈ 0.60 t
Piping dia. 8" - 4"		≈ 0.25 t
Valve – Flanges dia. 18"		≈ 2.25 t
Valves – Flanges dia. 8" - 4"		≈ 1.60 t
Total		≈ 62.00 t

3.2 Total buoyancy

Concrete block	$6.85 \text{ m} * 4.50 \text{ m} * 0.70 \text{ m} * 1.03 \text{ t/m}^3$	$\cong 22.20 \text{ t}$
Piping dia 18"	$0.452 \pi/4 * 7.00 \text{ m} * 1.03 \text{ t/m}^3$	$\cong 1.15 \text{ t}$
Piping dia 8" - 4"	$0.222 \pi/4 * 9.00 \text{ m} * 1.03 \text{ t/m}^3$	$\cong 0.28 \text{ t}$
Valves and flanges	Negligible	
Total		$\approx 23.60 \text{ t}$

3.3 Net Weight with empty pipes

Weight	$= 62.00 \text{ t}$
Buoyancy	$= -23.60 \text{ t}$
Total	$\approx 38.40 \text{ t}$

3.4 Net Weight with filled pipes (In-service)

Weight	$= 62.00 \text{ t}$
Buoyancy	$= -23.60 \text{ t}$
Fuel	$0.95 + 0.19 + 0.05 = 1.20 \text{ t}$
Total	$\approx 39.60 \text{ t}$

As a result of the above calculation:

$$\text{Weight} / \text{Buoyancy} = 62.00 / 23.60 = 2.627$$

4. Limit Equilibrium

The following forces could be applied on the PLEM structure:

- Line pressure (15 barg)

$$R_1 = 1507 \text{ cm}^2 * 15 \text{ barg} = 22600 \text{ kg} = 22.6 \text{ t}$$

(horizontal applied) [3]

- Pulling forces by submarine hoses (breaking load)

$$R_2 = 12000 \text{ kg} * \text{no. } 2 = 24000 \text{ kg} = 24.0 \text{ t}$$

(Applied over 25 m / 15 m i.e. - $\alpha = 25^\circ$) [3]

Drag forces are negligible in reason of partially embedded condition. The following stabilizing forces can be considered.

Horizontal stabilizing forces:

- Horizontal Base sliding (with sliding factor = 0.7 for sandy / gravely material) [4]

$$S_b = \text{Net weight} * 0.7 = 39.60 \text{ t} * 0.7 * 1.1 = 30.5 \text{ t}$$

- Soil Passive pressure, with .70 m of embedment ($\phi = 35^\circ$, $K_p = 3$, $\gamma' = 0.9 \text{ t/m}^3$) [4]

$$S_f = 0.5 * \gamma' * .702 \text{ m} * K_p * 4.5 \text{ m} = 0.5 * 0.9 * .702 * 3 * 4.5$$

$$S_f = 3.0 \text{ t}$$

$$S_{\text{tot}} = S_b + S_f = 33.5 \text{ t}$$

Based on the aforementioned forces the following conditions are verified:

- Horizontal equilibrium:

$$F_s = S_{\text{tot}} / (R_2 * \cos \alpha) = 33.5 / (24.0 * 0.9) = 1.55$$

- Vertical equilibrium

$$F_s = W / (R_2 * \sin \alpha) = 39.6 / (24.0 * 0.42) = 3.9$$

- Rotation equilibrium

$$M_{fl} = R_2 * \sin \alpha * 4.5 = 24.0 * 0.42 * 4.5 = 45.4 \text{ tm}$$

$$M_{s1} = W * 4.5 / 2 = 39.6 * 4.5 / 2 = 89.1 \text{ tm}$$

$$F_s = M_{s1} / M_{fl} = 89.1 / 45.4 = 1.96$$

5. Average Soil Pressure

The average soil pressure is:

$$p = W / A = 39.6 / (6.85 * 4.50) = 1.285 \text{ t/m}^2 = 0.13 \text{ kg/cm}^2$$

Which is admissible for sandy soil layer.

6. Results and discussion

Global PLEM behaviour under lateral loads has been modelled using the APILE2 software [5], which considers the non-linear behaviour of the soil by means of nonlinear horizontal load-displacement (p-y) curves.

The horizontal, vertical and rotation equilibrium was checked taking in consideration all the forces acting in the structure and considering the two cases; when the pipeline is empty and when the pipeline is filled with fuel oil (in service). The different buoyancy was considered for the two cases.

The safety factors are as below:

- For the horizontal equilibrium: $F_s = 1.55$
- For the vertical equilibrium: $F_s = 3.9$
- For the rotation equilibrium: $F_s = 1.96$

7. Conclusion

After a geotechnical inspection to define foundation type, in this paper it is analysed the stability of the PLEM. The PLEM is a steel frame connected with to the pipeline by means of a spool piece with flanged connection and with two submarine hoses.

There are considered two load combinations, and the result of this analysis is that PLEM structure is steady and reliable.

The offshore installation of the pipeline and PLEM was carried out successfully and the system for fuel oil unloading is still working properly.

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Assessment of the risk of electromagnetic emissions through monitoring for an object on the territory of the Metropolitan municipality

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Abstract: In many cases, the importance of electromagnetic fields or the so-called "electromagnetic smog" on human health, living organisms, and the environment is ignored or at least minimized. The electromagnetic field (EMF) is a combination of invisible electric and magnetic fields with charge that occur in nature. Today, the impact of electromagnetic fields on the environment is increasing from human activities with the development and application of telecommunication technologies. Measurement, continuous monitoring, database creation, and evaluation of electromagnetic field parameters in urban environments are important aspects of optimizing EMF levels to achieve a healthy living environment. The paper presents an analysis and evaluation of electromagnetic radiation measurements from base stations of mobile operators, at one site, in a lightly urbanized urban environment over a certain time interval. Initial measurements showed exceeding the levels of electromagnetic emissions according to national legislation. During the repeated measurements, it was found that mobile operators, after correcting the radiation from base station antennas, have brought the radiation levels within the permissible limits. This underscores the necessity of ongoing monitoring.

Keywords: ELECTROMAGNETIC RADIATION, ELECTRIC FIELD, ELECTROMAGNETIC POLLUTION, ENVIRONMENTAL LEGISLATION, EMF MONITORING

1. Introduction

The rapid development of the telecommunications industry has led to an oversaturation of the environment, especially the urbanized one, with technical sources of electromagnetic radiation. Electromagnetic radiation (EMR) is a wave propagating through space with an electric and magnetic component. Communication devices such as: mobile phones, cordless phones, local wireless networks and radio transmission towers, provide an electromagnetic field (emission) at the time of receiving and transmitting information. Existing and new radio communication technologies are leading to an increase in the number of base stations. When these facilities are placed in close proximity to or on public and residential buildings, in order to ensure greater traffic capacity and full radio coverage of the mobile phone service area, their penetrating radiation increases the voltage in the aforementioned buildings many times over. 2G, 3G, 4G, 5G antennas, with low and high range, continuously broadcasting the signals of mobile operators penetrate our homes and workplaces and can pose a serious threat to our health. Electromagnetic radiation is invisible to us, we cannot see, taste, or feel it, and this makes it particularly dangerous, as we have no way to protect ourselves from it. Radiation (EMF) passes through material or a person's skin, causing ionization that can damage chemical structures, and if the damage occurs to critical chemicals in cells (such as the DNA molecules that make up chromosomes in the cell nucleus), the cell can become damaged and lead to the formation of cancer [1-5].

In many cases, the importance of electromagnetic fields or the so-called "electromagnetic smog" on human health, living organisms, and the environment is ignored or at least minimized. European environmental policy currently includes more than 300 legal acts, including directives, regulations, decisions and recommendations [6,7]. They cover a wide range of measures to protect the environment from pollution through standards for air quality, waste management, water, nature conservation, control of industrial pollution, chemicals, background noise and climate change. Unfortunately, despite the growing public interest and concern, not enough attention is still paid to electromagnetic fields of artificial origin as a pollutant and the effects they cause on the environment, human health and living organisms. The fact is that the level of radiation from artificially created EMI in a number of cases significantly exceeds the level of electromagnetic fields from natural sources. Therefore, monitoring is necessary. To verify, assess, control and protect the population and the environment from exposure to electromagnetic radiation in accordance with the national standards and regulatory requirements adopted for each country [8-10].

Monitoring can be done for: source monitoring, environmental monitoring and individual monitoring, as appropriate. In this work we show individual monitoring. The rapid urbanization of the city

of Bankya, Sofia Municipality, Bulgaria, and the modern world's need for communication devices have led to the number of transmitting antennas and their power exceeding the maximum permissible standards in the Site under consideration. The purpose of this type of monitoring is to verify and assess the dose of electromagnetic fields received from the base station RADOST - SID SOF0318 with three sector antennas (A1: KATHREIN 80010292, A2: KATHREIN 742266, A3: KATHREIN 742266) near the studied site. Does the facility fall within the hygienically protected zone within the meaning of regulation [8] of the Bulgarian legislation on the maximum permissible levels of electromagnetic fields in populated areas and the determination of hygienically protected zones around radiating facilities?

2. Characterization methods

Two types of specialized mobile equipment were used. One of the devices is SPECTRAN, manufactured by the German company AARONIA AG, including a SPECTRAN HF 6060 device for measurements in the range of 1 MHz to 7 GHz, with a directional antenna type HyperLOG 7060. The measurement results are visualized in real time and recorded on a laptop computer on which the manufacturer's software is installed, allowing for settings, measurements, visualization and storage of the results in graphical and digital form.

The device was set to a measurement frequency range - from 700 MHz to 2500 MHz, to cover the emission range from the RADOST Base Station - SID SOF0318. The device is equipped with a directional HyperLOG 7060 antenna. It was set to a measurement frequency range of 0.68 – 8 GHz. Measurement data is recorded every 1000 ms. The data are stored in digital files and are combined with additional accompanying information, such as coordinates, a map of the area, location of the antennas, records of the spectrograms in graphical form for each measurement interval, and photographs of the measurement locations with the installed antennas.

The second device is the NARDA AMB 8057-03, for electromagnetic monitoring of the environment in the frequency range from 110 kHz to 7 GHz, providing capabilities for continuous autonomous control of electromagnetic radiation, even in points difficult to access for observation. It uses isotropic antennas to measure the characteristics of electromagnetic fields propagating in the environment and reaching the antenna from all directions in space (360°). The transmission of data to the server with the database is automatic, and there is also the possibility of remote setting of the device. The measurement results are stored in a specialized database containing information about the characteristics, frequency distribution and location (coordinates of the measurement points in a geographic information system GIS) of electromagnetic radiation. Mobile equipment NARDA AMB –

8057-3 provides capabilities for visualizing and reviewing measurement information for a selected time interval, date, week or day, for the entire frequency range (110 kHz – 7 GHz), a selected subrange of the frequency range (110 kHz – 7 GHz), peak values or average value for a specific frequency, a selected time interval of the day, to observe subtle effects in the distribution of radiated power for a given frequency.

3. Results and Discussion

Object - town Bankya, Sofia Municipality, Bulgaria. Measurements of electromagnetic fields in the frequency range 700 MHz to 2500 MHz were carried out with mobile equipment SPECTRAN HF 6060 in the time interval from 11:17 to 11:35 on 21.10.2015 (Fig.1). The measurement lasts for 18 minutes. The point from which the measurements were taken at the site is located ~ 25-30 meters in a straight line from the transmitting antennas of the base station. It should be noted that the measurements were conducted in a residential area, on a weekday, Wednesday. The area is without public and administrative buildings and it is assumed that the use of mobile communications is characterized by low traffic.



Fig. 1 Location of measuring equipment and arrangement of base stations around the research site.

Figure 2 shows the average values of the measurement results in three frequency ranges 943 MHz, 1690 MHz and 2152 MHz. Maxima are observed in the levels of measured EMI around 267 $\mu\text{W}/\text{cm}^2$, at a frequency of 943 MHz. These measured values are twenty times above the permissible level of 10 $\mu\text{W}/\text{cm}^2$, according to [8]. Excessive electromagnetic pollution has been measured. At frequencies 1690 MHz and 2152 MHz, the measured values are below the permissible level of 10 $\mu\text{W}/\text{cm}^2$, 0.8990 $\mu\text{W}/\text{cm}^2$ and 0.5070 $\mu\text{W}/\text{cm}^2$, respectively.

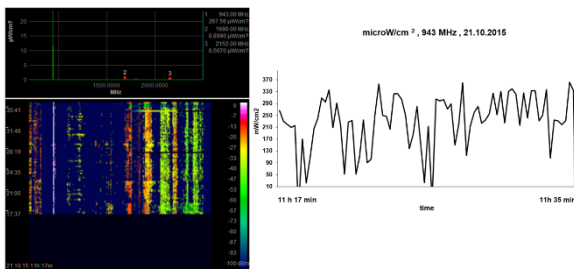


Fig. 2 Average values of the results of measuring the energy flux density of EMF.

In the same object, city Bankya, Metropolitan Municipality, Bulgaria, repeated continuous 24-hour measurements of electromagnetic fields with another mobile device NARDA AMB-8057-03 were carried out from May 27 to June 9, 2021 in the frequency range 100 kHz to 7 GHz. This frequency range covers the frequencies of all mobile operators operating on the territory of Bulgaria and allows to track the dynamics of electromagnetic radiation from the antennas, on weekdays and weekends. The point from which the measurements were made in the object is located ~ 25-30 meters in a straight line from the emitting antennas of a base station.

Figure 3 shows the average values of the measurement results in three frequency ranges 942 MHz, 1842 MHz and 2140

MHz. These frequency ranges were selected based on a recommendation from the EU Council [7].

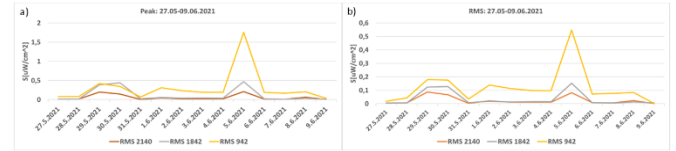


Fig. 3 Maximum peak values (a) and maximum average values (b) from measurements of electromagnetic fields in an object.

The maximum peak values of the measured power density of electromagnetic radiation EMF, for two of the frequency ranges 1842 MHz and 2140 MHz, are almost an order of magnitude lower than the maximum permissible standard of 10 $\mu\text{W}/\text{cm}^2$. For a frequency of 1842 MHz, the measured values range from 0.011 to 0.47 $\mu\text{W}/\text{cm}^2$, and for a frequency of 2140 MHz, they range from 0.007 to 0.21 $\mu\text{W}/\text{cm}^2$. For the 942 MHz frequency range, the measured values vary in a wide range from 0.03 to 1.75 $\mu\text{W}/\text{cm}^2$. For this frequency range, we should pay attention to the values obtained on June 5. This date is a holiday (Saturday).

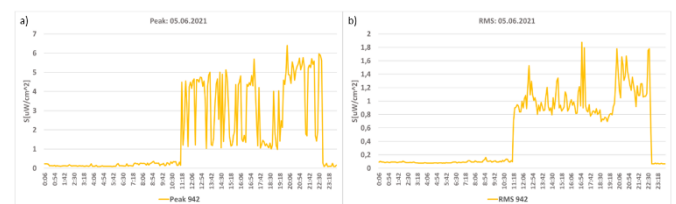


Fig. 4 Maximum peak values (a) and maximum average values (b) from measurements of electromagnetic fields in the facility for 05.06.2021.

Figure 4 shows the maximum peak and average values measured on 05.06.2021. The maximum peak values range from 0.083 to 6.39 $\mu\text{W}/\text{cm}^2$. It is noteworthy that the highest values were measured in the evening hours, and after 10:30 p.m. the power of electromagnetic radiation decreases sharply.

As we have already specified, this is a residential area and the use of mobile sources on weekends is higher both for the date 05.06.2021 and the previous Saturday 29.05.2021. Accordingly, the level of the measured power density of electromagnetic radiation EMF has increased, but the obtained values are below the maximum permissible standard value of 10 $\mu\text{W}/\text{cm}^2$.

The maximum average values of the measured power density of electromagnetic radiation EMF, for a frequency of 2140 MHz - vary from 0.002 to 0.08 $\mu\text{W}/\text{cm}^2$. For frequencies of 1842 MHz and 942 MHz they vary in a wide range. For frequency 1842 MHz they vary from 0.004 to 0.15 $\mu\text{W}/\text{cm}^2$, and for frequency range 942 MHz, from 0.002 to 0.17 $\mu\text{W}/\text{cm}^2$. The measured values for the three frequencies are less than the standard limit values of 10 $\mu\text{W}/\text{cm}^2$. Again, if we pay attention to the date 05.06.2021, Saturday, the measured values for the 942 MHz frequency range are higher. They are in the range of 0.07 to 1.87 $\mu\text{W}/\text{cm}^2$, which is less than the maximum permissible values according to the standard of 10 $\mu\text{W}/\text{cm}^2$.

From the measured power density of electromagnetic radiation EMF for all fourteen days of measurements, a recurring daily trend of a decline in the levels of radiated power from mobile operators is observed, after 10:30 p.m. to 6:00 a.m. - 6:30 a.m. This is explained by the end of the day and the intensive use of mobile communication devices. This trend of difference is repeated when comparing workdays and weekends.

Conclusions

The results obtained from measuring electromagnetic radiation in a site located in a lightly urbanized environment prove to us the need for monitoring. To carry out regular measurements and thus to check and control the parameters of all sources of EMF. To seek solutions to the problem of environmental pollution with this new

pollutant, including methods for analyzing the risk to human health and related solutions for the development of urban infrastructure.

During the initial measurements of electromagnetic fields in the frequency range of 943 MHz, maxima in the levels of measured EMF were observed $\sim 267 \mu\text{W}/\text{cm}^2$. These measured values are more than twenty times the permissible level of $10 \mu\text{W}/\text{cm}^2$. Excessive electromagnetic pollution has been detected. For the frequency ranges 1690 MHz and 2152 MHz, the measured values are below the permissible level of $10 \mu\text{W}/\text{cm}^2$. Cases of exceeding the maximum permissible standards under the Bulgarian National Standards in individual sites on the territory of Sofia Municipality are rather an exception. The presence of isolated cases of exceeding the permissible norms may be due to the degree of urbanization, inconsistency between the construction control authorities and those controlling the installation of new broadcasting antennas, their power and location. All this has a significant impact on the intensity of electromagnetic emissions. In this case, one of the frequencies at the date of measurement was not set to operate within the permissible radiated power level of $10 \mu\text{W}/\text{cm}^2$ [8].

During repeated continuous, all-day and continuous monitoring of fourteen days in the same site at the request of citizens and in the three frequency ranges 942 MHz, 1842 MHz and 2140 MHz, the levels of the measured radiated power of electromagnetic radiation are below the maximum permissible norm of $10 \mu\text{W}/\text{cm}^2$. This proves that after the initial measurements with high values, SRHI took action to eliminate the discrepancies, in accordance with the procedure specified in the regulatory framework and they were eliminated. Therefore, it is crucial to conduct monitoring and control of EMF to ensure a high level of protection of the population and the proper functioning of the Base Stations within the permissible norms.

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Large-scale distortion analysis of the welding and thermal straightening process chain

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Abstract: An coupled analytic-numerical model for calculation of distortions arising by welding fabrication is introduced. Target of the analytical model is the calculation of the inherent strains after the local thermal-mechanical influence of the welding or thermal straightening process. Following the fabrication processing chart the strains are loaded on an elastic FE-model of the structure and the residual stresses and distortions of the whole structure are calculated. The consideration of welding and thermal straightening scenarios, inclusively the assembling stages, is done by taking into consideration the intermediate variation of the strain state in the FE-model of the structure at every processing step. The important physical relations are demonstrated. The model is intended to be used for solving industrial tasks, i.e. intending acceptable precision and calculation time as well as low simulation costs.

Keywords: COMPUTATIONAL WELDING MECHANICS, WELDING DISTORTIONS, THERMAL STRAIGHTENING, INHERENT STRAIN, FE-SIMULATION,

1. Introduction

The welding distortions are still one of the main problems in welding technology. It decreases the quality of the welded structure, complicate the smoothly accomplishment of the manufacturing process and increase the cost of post treatments. Since several decades, thermal straightening is an established technique for improving welding structures, figure 1. By the thermal straightening additional deformations are set into the structure, which lead to decreasing the out-of-plane distortion and increasing the in-plane shrinkage. Prediction and control of the welding distortions and the effect of the thermal straightening process, during the early stages of a designing cycle, allows avoiding expensive engineering correction that could occur later.



Fig. 1 Railway carriage after thermal straightening, ©ALSTOM Transport Deutschland GmbH, Salzgitter[1].

In the praxis, the straightening process guidance and parameters are chosen on the background of practical experience, complemented with additional time- and cost-intensive experiments. Unfortunately, it is impossible to capture the influence of the different structural stiffness, materials and residual stresses state after welding with this approach.

Another approach seems to be a welding - thermal straightening distortion simulation, based on the computational welding mechanics. Particularly in welding, analytical [1-8] or numerical [9-15] models achieve a solution to the problem. The application of analytical models is strongly limited due to the necessary simplifications of the physical phenomena. However, the numerical models are free from such limitations. They cover a large number of topics; one of which is the prediction of the residual distortions caused by the mutual influence of highly localized heat input, the material behavior, the initial inhomogeneous temperature and stress-strain states and the response of the whole structure as well.

Despite the benefits the numerical models, one great difficulty of their industrial utilization is the complexity of the problem formulation and necessity of number of input data that, in many cases, are not available. In addition, the conventional numerical thermal-mechanical welding simulations, combined with straightening simulations are not feasible, as they require significant computation costs and highly qualified personnel. These difficulties rise when the subject of the simulation is a large, complex welded structure, which often makes the employment of numerical methods in the praxis inefficient. Hence, there is a great demand for simple welding simulation models and approaches, which allow for quick distortions prediction. The technical literature gives among the works [5-9] a set of validated applications that are limited to simple welded joints [14-17].

To answer this need, an innovative solution approach for simulation of welding distortions has been developed in earlier works [16,18]. The approach is an integration of analytical and numerical calculation's procedures in a hybrid model. The analytical procedure calculates the shrinkage volume and strains distribution in it, representing the weld seams, based on the theoretical works of Okerblom [2], Gatovskij [4] and Vinokurov [5] implemented in a novel enhanced model [18]. The calculated strains are then applied to the global FE model in order to predict the distortions of the structure during the welding stages, as well as their residual state [18].

The aim of this article is to demonstrate the analytical models for calculation of inherent strain by welding and thermal straightening processes as well as their implementation it into the welding simulation procedure. Thus, to suggest an engineering tool that reduces the development time, costs and increases the efficiency of manufacturing.

2. Concept of the coupled analytical-numerical model

By welding, the analytical approach calculates the shrinkage volume, representing the weld seams, based on the theoretical works [3-7]. The introduction of complex semi-empirical relations allows to take into consideration the relevant factors and to reach the need adequacy. The calculated plastic strains are then loaded (as inherent strain) on the global FE model in order to calculate the distortions of the structure during the welding (after every one weld), as well as the global stress field. For this purpose, an elastic FE-analysis, which is fast and needs a simple material data definition, is carried out. The stress state in the structure, obtained from the numerical calculation, returns to the analytical one, using a loop statement for every next weld. This procedure allows taking into consideration the influence of the welding sequence as well as the changes of the clamping conditions and variation of the structure restrain.

The solution of the deformations problem, in the thermal straightening, bases on the hypothesis for prolonged accumulation of plastic strains, caused by the irregular thermal-deformational

field around the heat source, the restrain intensity and the variation of the stress state as well. This govern the basic approach for solving such problem (figure 2), in which a thermal-deformation calculation is carried out, taking into account the nonlinear material behaviour, as well as the field of initial elastic strains. The last one could be due to residual stresses, caused either after welding or already completed straightening works as well. That means the simulation of straightening follows the simulation of welding and consists of sequential calculations of plastic, i.e. inherent, strain of every one heat spot.

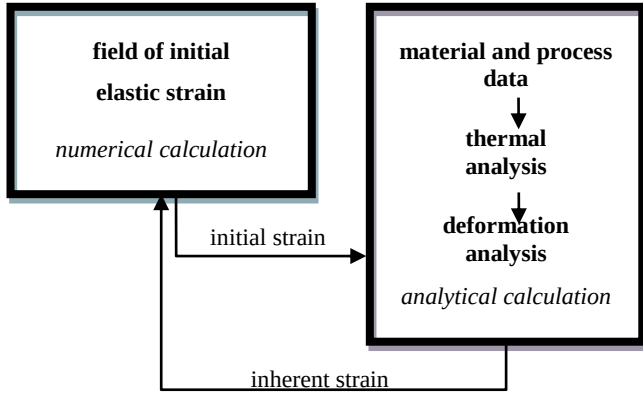


Fig. 2 Coupled analytical-numerical analysis

On this place is not worth to underline that the both, welding and thermal straightening simulations may be executed in an arbitrary sequence, following various manufacturing charts of the fabrication plan.

3. Analytical model

3.1 Welding

Purpose of the analytical model (also shrinkage model) is determination of the area where plastic strain appears. As mentioned above, scientific background of the model for welding build the plastic strains evolution under temperature dependent ideal elastic-plastic material behavior [3,5-7]. In case of long welds, the mechanical problem can be considered as uniform stress-strain in the weld cross section with known distribution of the maximal temperatures. The thermal strain (expansion and shrinkage) in the longitudinal direction is constraint by the surrounding cold metal, which realizes infinity restrain. After this model the zone with the plastic strains w_{pz} is defined by certain maximal temperatures, i.e. by the maximal thermal strain $\alpha(\Delta T_{max})$ equal to yield strain ε_Y plus initial strain ε_0 and $2\varepsilon_Y$, figure 3.

For the longitudinal component of the inherent strain, it is govern by the equation:

$$\varepsilon_x^* w_{pz} s = \int_0^s \int_0^\infty \varepsilon_x^{pl} dy dz = s \int_{\varepsilon_Y + \varepsilon_0}^{2\varepsilon_Y} y(\varepsilon_{max}^{th}) d\varepsilon_{max}^{th},$$

and for the transverse component, respectively:

$$\varepsilon_y^* = \frac{1}{w_{pz} s} \int_0^s \int_0^\infty \varepsilon_y^{pl} dy dz.$$

Herby the ε^* is the inherent strain to the corresponding component direction (x or y), s is the thickness of the sheets, w_{pz} is the width of the plastic deformed zone, ε_Y is the strain at the yield stress, ε_0 is the initial strain (that is elastic in nature), ε^{pl} are the plastic strain components (x or y), ε_{max}^{th} is the thermal strain at the maximal temperature.

Different influences on plastic strains, for example relation of the heat input to the thickness, external and internal stiffness, strain hardening, existing plastic strains in the weld, solid state transformations, clamping conditions or multi-run welding can be considered by semi-empirical correction functions [6,17]. Capturing all these effects on the arising distortions simultaneously is not possible as they also affect each other. In the technical literature is among the works [5-9] a set of applications published and compared with experimental results for simple welded joints [14-17] or large [16,18].

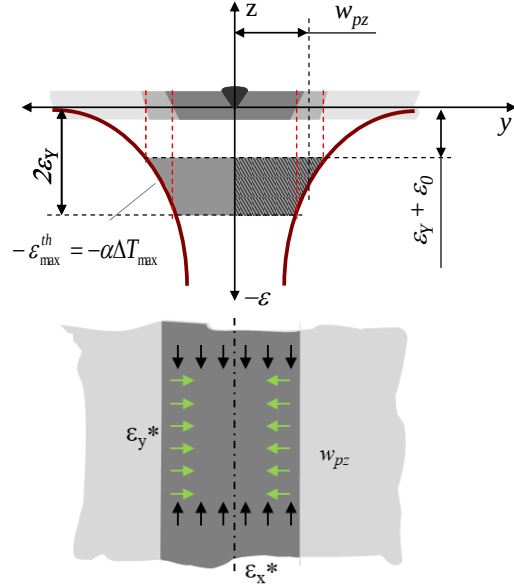


Fig. 3 Formation of plastic strain by welding

The distribution of maximal temperatures can be obtained by derivation of the quasi-stationary temperature field. In that order, diverse analytical solution can be used. The current model for welding, for example, uses a combination of moving point heat source in semi-infinity body and moving line heat source in thin plate [18,19]. The combination factor depends on the welding process, heat input and the thickness of the sheets.

3.2 Heat spot

If the heat source is fixed, as it by thermal straightening with heat spot is, the kinetics of plastic strain evolution vary. Assuming a large circular disk, subject of heating at its center and followed by cooling, an axial symmetric mechanical problem should be considered, figure 4.

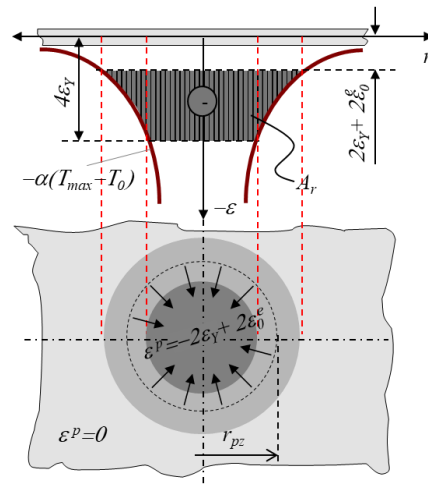


Fig. 4 Radial distribution of the plastic strains after heat spot.

In this case, the surrounding cold metal behaves elastically during both heating and cooling, and the strain occurring in it is equal to the elastic strain in the heated core. Hence, in a stress free disc the plastic strains arise in domain between maximal thermal strain $\alpha(\Delta T_{max})$ equal to yield strain $2\varepsilon_Y + 2\varepsilon_0$ and $4\varepsilon_Y$ respectively, see plastic deformed zone r_{pz} on figure 4. Presence of any initial strain will shift the thermal strain curves with its value and sign, like to heat line problem [21,22]. Thus, the shrinkage volume (v_r in $\text{mm}^2 \text{ mm/mm}$) in case of heat spot is given by the equation:

$$v_r = \int_{(2\varepsilon_Y + 2\varepsilon_0)}^{4\varepsilon_Y} A_r d\varepsilon^{th}$$

where A_r is the area bordered by the maximal thermal strain and integral limits $2\varepsilon_Y + 2\varepsilon_0$ and $4\varepsilon_Y$ respectively. Hence, the radius of plastic deformed zone is:

$$r_{pz} = \sqrt{\frac{v_r}{\pi(2\varepsilon_Y + 2\varepsilon_0)}}$$

To apply the model in the overall calculation procedure an appropriate solution of the thermal problem is need. In the common case, the solutions with instantaneous line heat source [19-23], enhanced to non-uniform initial temperature T_0 , suggested by [24] give satisfactory results.

3. Application of the analytical-numerical model

To demonstrate the capability of the coupled analytical-numerical model a welding simulation, followed by simulation of a thermal straightening is carried out. The subject is a qualitative demonstration, i.e. plausibility proof of the model. To do it, a complex structure with 8 welds was chosen [20], figure 4. It is made from two T-profiles being joined with the plate by weld 1 and 2, two L-profiles joined with the plate by weld 3 and 4 and with the T-profiles by the welds 5, 6, 7 and 8. Each wall thickness is 3 mm. The distance between T-profiles is 800 mm. The material is a mild structural steel. For the purpose of simulation, reference welding process and parameters are chosen.

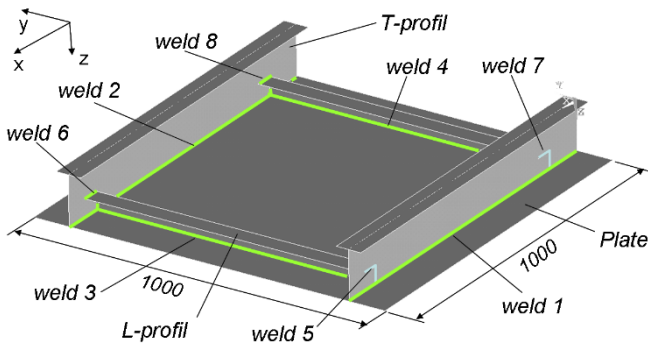


Fig. 5 Model of the complex structure with dimensions and welds (upside down)

Object of the analysis are the distortions in the sheet, closed by the frame. The blades are not considered by the analysis. According to [20] great angular distortions should be expected if at the beginning the L-profile and the T-profile are welded together (welds 5 – 8), afterwards they are joined to the sheet by welds 1 and 2 simultaneously and finally by 3 and 4.

For the distortion calculation using the hybrid model a finite element model with shell elements was generated. For the purpose of simulation, the mesh is very coarse and just refined in the supposed areas of plastic deformation, where the structure is loaded. Thus, in those domains the element size is about 20 mm (according to the analytical solution). For the reason of free forming, only the node in the middle of the plate ($x=500, y=500$) has fixed DOF (translation and rotation). The loading is realized by longitudinal

and transverse strains, linear distributed along the thickness, in three steps according to the welding scenario.

The strains in the longitudinal and transversal directions, as well as their centroid are calculated by means of the analytical model, stated above, under stress free condition. At this stage, a reference temperature distribution is applied. For the third step, the residual stresses after the second step (tension in the plate and compression in L-profile along welds 3 and 4) are taken into consideration. The obtained results are presented exemplary by the field of displacement in Z-direction (out-of-plane), figure 6.

The distortions, obtained by the coupled analytical-numerical model satisfy qualitatively the expected [20]. The average values of calculated bending and transverse shrinkage in the plate, between the T-profiles, in the middle of the structure, after welding are as follow: $\Delta Z = 3 \text{ mm}$ and $\Delta Y = 0,2 \text{ mm}$.

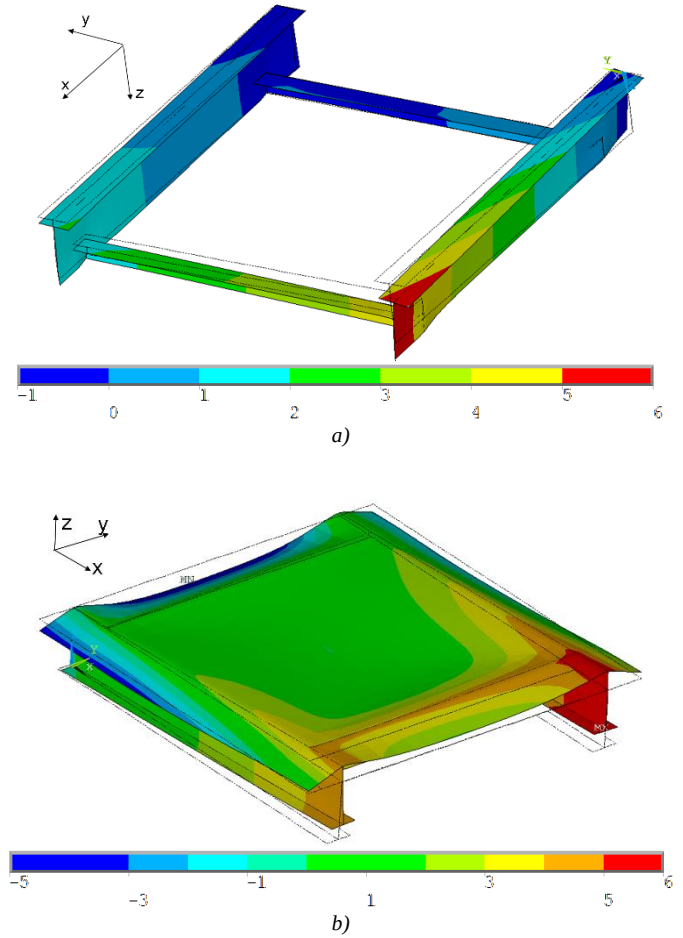


Fig. 6 Bending of the structure (displacement in z-direction, mm), scaling x75: (a) after welding the profiles, (b) after welding entire structure

Based on the calculated welding distortions a thermal straightening simulation is carried out. It is done by means of 46 heat spots, with distance about 100 mm each other. The process parameters are also reference, so that the nominal width of plastic zone corresponds to about 20 mm. The heating sequence follows a spiral trajectory, from outside to the middle, in 46 load steps, figure 7.

The inherent strains for every next spot and direction are recalculated, taking into consideration the actual strain (or stress) vector from the current element. Hence, it varies from spot to spot in the range between several times and zero. At this stage the recalculation was done according to the relation from [18,24]. The calculation time for the entire simulation, welding and straightening, took 5 – 10 min. The obtained results, after the last

spot, confirm the expected result – straightening the sheet (figure 8) and accumulating additional shrinkage in the plane. The calculation shows a reduction of the bending line to less than 1 mm, figure 9 (a). The in-plane shrinkage of the structure (exemplar distance between the T-profiles), rise from 0,2 mm to 0,25 mm (figure 9 (b), blue curve).

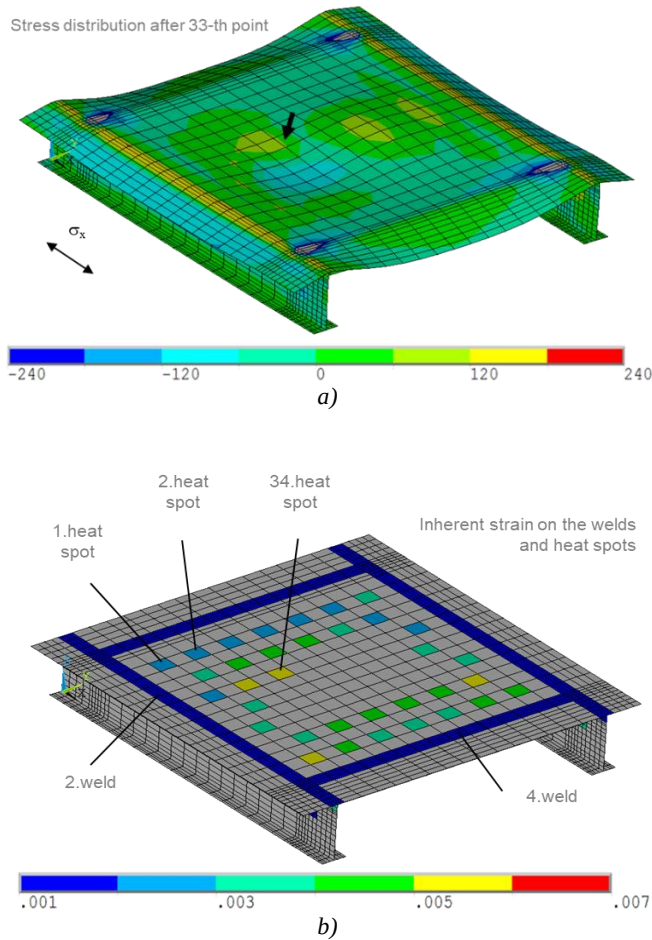


Fig. 7 Loading of the 34 heat spot: a) stress field (in MPa) after 33. heat spot, b) inherent strain, loaded to the elements at 34. heat spot

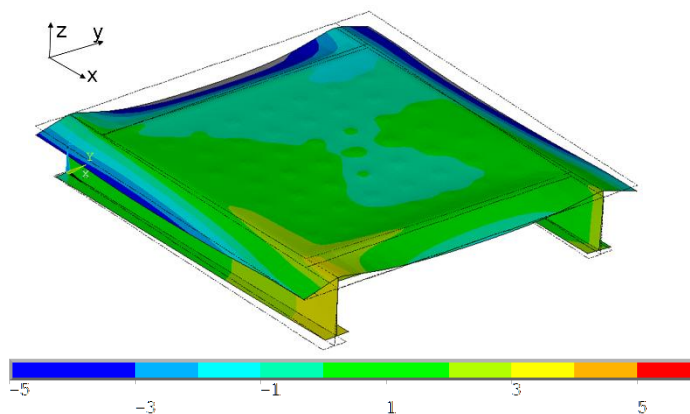


Fig. 8 Bending of the structure after straightening (displacement in z-direction, mm), scaling x75

The examination of intermediate steps shows that not all heat spots are reasonable, i.e. some of them don't affect the distortions or cause a negative effect. For example, the angular distortions after heat spot 41 are equal to the final and after 44 spot they reach the minimum. Never less, it is an evidence that the model provides

possibilities to determine the structural shrinkage as well as to analyze or optimize the straightening procedure.

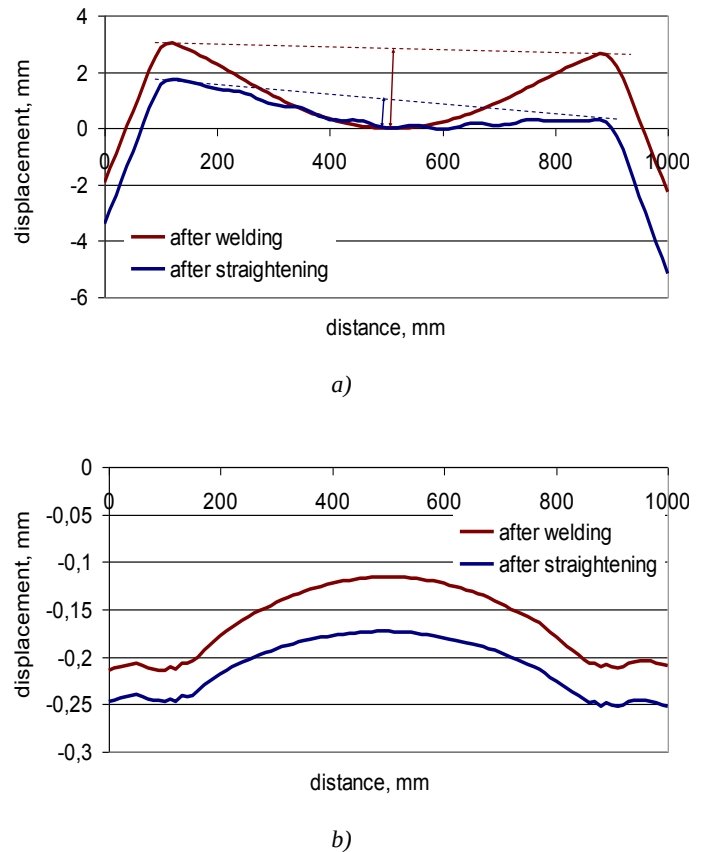


Fig. 9 Comparison between "after welding" and "after straightening": (a) out-of-plane displacement, (b) transverse shrinkage

4. Conclusions

To find the optimal process guiding and parameters for a thermal straightening of welded structures as well as the resulting shrinkage, the procedure of time- and cost-intensive experiments is still commonly used. For this purpose, the employment of the contemporary thermal-mechanical finite-element-simulation is increasing. However, in range of large structures with multiple weld seams, the complete thermal-mechanical FE-simulation is not rational.

The limitations of the common thermal-mechanical FE-simulation, regarding the distortions calculation of large welded structures are summarized. An abstract about the existing simplified approaches as well as their inadequateness is given. Based on the need for fast and adequate precise approaches for calculating welding distortions in large structures a novel and practically relevant model is suggested and demonstrated.

To demonstrate model capability an analysis of welding distortions, after welding and subsequent thermal straightening, in a large structure has been carried out. Furthermore, the qualitative interaction of the thermal straightening and the welding processes is analyzed for one reference scenario. For this purpose, the variation of the stress state in the structure, during welding and straightening is taken into consideration. The results of the analytic-numerical hybrid model match the expertise and the expectations.

The example is performed using ANSYS® FE-code. The calculation time was about several minutes, on a common PC. The coupled analytic-numerical model shows enormous potential regarding to the reduction of the computation time, as well as significant reduction of the pre-processing time. However, there is a

demand for future research and development activities over finding the proper equation for the maximal temperature distribution, considering the most used flame and induction heat sources, as well as cooling conditions due to fixing tools. In addition, the process parameters, derivation of the material parameters for different structural materials as well as different numerical model parameters should be characterised.

Abbreviations

DOF	degree of freedom
IP	integration points
A_r	is the area bordered by the maximal thermal strain and integral limits $2\varepsilon_Y + 2\varepsilon_0$ and $4\varepsilon_Y$ respectively
b_{pz}	width of the plastic deformed zone
r_{pz}	radius of the plastic deformed zone
v	shrinkage volume
ΔY	overall in-plane shrinkage in Y-direction
ΔZ	maximal out-of plane displacement
ΔT_{max}	maximal temperature increment
T_0	initial temperature
α	thermal expansion coefficient
ε_Y	strain by yield limit
ε_0	initial elastic strain (residual strain from preliminary load steps)
ε^*	inherent strain
ε^{el}	elastic strain
ε^{pl}	plastic strain
ε^{th}	thermal strain

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Investigation of extruded polyethylene blend films tensile creep elongation with different open holes configurations

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Abstract: In this article, tensile creep elongation of PE plastic films with two configurations of open holes stressed at the incremental temperatures of 23,30,37,43,51,58 degrees and a constant dead weight of 50 kg will be analyzed. The aim of the article is to analyze influence of the two configurations of open holes on the relative elongation. The test was conducted with the same blend, under the same test conditions. The test was conducted entirely according to the conditions established in the ASTM D6992 standard.

Keywords: CREEP TESTING, TENSILE STRENGTH, ELONGATION, PLASTIC FILMS.

1. Introduction

In engineering practice, there is often the presence of parts with open holes[1]. The presence of such holes is most often due to the following technological reasons:

- Lightening structure
- Access to various fasteners
- Access to other parts of assembled units
- Access for repair work
- Access for electrical switches, etc.
- Functional dimensions for assembly

Parts with open holes have lower strength properties than those without holes because stress concentrators are created and cross section is reduced [2]. In solid mechanics, stress concentrators are locations in a part where the stresses are greater than the surrounding area (Fig. 1). Stress concentrators include holes, grooves, notches, scratches, etc. Stress concentrators also reduce fatigue strength.

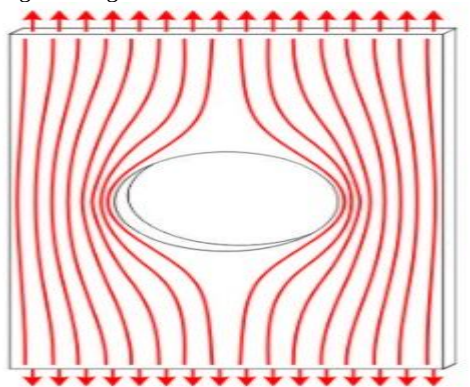


Fig.1 Internal force lines

It is known that the size and configuration of the holes play an important role in elastic properties of the material[3]. The dimensionless stress concentration factor k is used, which is equal to the maximum stress divided by the nominal stress. For round holes, the coefficient is equal to 3, i.e. the stresses along the edge of the hole are three times higher than the nominal stresses[4]. Despite the availability of CAE tools such as the boundary element method and finite element method predictive strength calculations with them through simulations are not particularly accurate[5]. A number of scientific articles study and optimize the influence of hole configuration on the tensile and compressive strength of various materials[6]. In engineering practice, the following solutions have become necessary to reduce the effect of stress concentrators:

- Optimizing holes
- Hole reinforcement
- Optimizing shapes of parts.

In plastic materials, localized plasticity occurs, while in brittle parts, stress concentrators lead to failure.

Polyethylene(PE) is the most common plastic worldwide[7]. Research into its mechanical, physical and chemical properties is very current. Polyethylene has wide application in industrial practice. Creep of materials is a slow increase in plastic deformation

caused by mechanical stresses[8]. It is an important characteristic of plastics because the process can start at room temperature[9].

2. Materials and methods

The material under the study is a PE blend, with a concentration of HDPE and LDPE 75 to 25 percent. It is characterized by low density, good chemical resistance, flexibility, easy processing by various methods, weldability, low hardness, high creep, impact resistance, excellent electrical insulator. PE is used as medical implants, packaging, pipes, bottles, ropes, etc. [10]. Products made of polyethylene are recyclable. The samples for testing have dimensions of thickness - 1.4 ± 0.01 mm, width 100 ± 1 mm, length 210 ± 1 mm. One sample has 26 open holes with a diameter of 10 ± 1 mm, and the other with 8 holes with a diameter of 20 ± 1 mm (Fig.2, Fig.3). The samples have technological holes of 8mm for installment (Fig. 4).

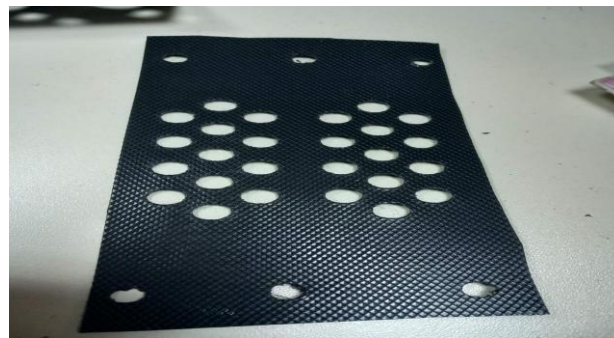


Fig.2 Sample with 26 open holes



Fig. 3. Sample with 8 holes

The area of the holes of the first and second samples is:

$$S_1 = 26(\pi \cdot r^2) = 2041 \text{ mm}^2 \quad (1)$$

$$S_2 = 8(\pi \cdot r^2) = 2512 \text{ mm}^2 \quad (2)$$

The area of the holes of the second sample is 23% larger. The samples were tested on a calibrated two-slot universal machine for creeping materials with appropriate jaws and scope. The test parameters are incremental temperatures 23,30,37,44,51,58⁰

degrees, dwell time at each step - 240 minutes - total test duration - 24 hours (fig. 5). the requirement that the test be longer than 60,000 seconds (app. 16.66 hours) has been met. The atmospheric conditions were observed according to the standard. The loading was carried out by weights of 50 kg, i.e. the force is equal to 490.5 N (Fig. 6).



Fig. 4 Installment of the samples in the chamber



Fig. 5 Parameters of the creep testing



Fig.6 Dead weight system

The test was performed on a calibrated creep machine. The test conditions specified in the standard were met. The temperature during the test was within the standard limits of $\pm 1^\circ\text{C}$. The selected incremental temperatures correspond to test conditions for heavily loaded structures. For the purposes of this article, only the elongation in mm after the test is considered, ignoring the elongation obtained at the different temperature steps. Both sockets

of the testing machine are used to ensure that both specimens are subjected to the same test conditions.

3. Results and discussion

The results of the accelerated creep test are presented in a table 1.

Table 1: Accelerated tensile creep results

Sample No	Elongation mm	Elongation %
1	14.06	6.69
2	9.06	4.31

The results show significant deformation of specimen 1, even though it is the specimen with the smaller hole area. The reason is that the linear force lines are denser in specimen 1 due to the selected hole configuration, fig. 7.



Fig. 7 Tested samples

The software of the universal creep machine records every 5 minutes and in the logs with the results there are no visible shock strains, which would compromise the test results (fig. 8) slippage or displacement of the test specimens is observed. No strain is observed in the gripping area. The strain is characterized by thinning around the horizontal axis of symmetry, forming an arc on both sides of the samples.

2024/11/13 13:09:11	13920	9060	58.1	"HOLES"	"	5.08	4.31	6.62
2024/11/13 13:14:11	13940	9060	58.1	"HOLES"	"	5.08	4.31	6.63
2024/11/13 13:19:11	13960	9060	58.1	"HOLES"	"	5.08	4.31	6.64
2024/11/13 13:24:11	14000	9060	58.0	"HOLES"	"	5.08	4.31	6.66
2024/11/13 13:29:11	14040	9060	58.0	"HOLES"	"	5.08	4.31	6.68
2024/11/13 13:34:11	14060	9060	57.9	"HOLES"	"	5.08	4.31	6.69

Fig. 8 Log file

The test results are significant, as plastics are increasingly used as a structural materials. Such tests can serve for more optimal designs of similar types of products with open holes [11].

4. Conclusions

. After testing the accelerated creep of the PE films blend with 2 different hole configurations, the following conclusions can be made:

- Sample 1, which has a smaller hole area, strains 55% more than sample 2
- The test conditions fully comply with the accelerated creep standard D6992
- No significant deviations were observed that would affect the test results
- The test results can be used for plastic films with a similar open hole configurations.

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Methods of Temperature Measurement in Heat Affected Zone during Welding

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Abstract: The metal materials are subject to severe temperature variation during the application of the welding process. The heat input and thermal shock during arc welding cause change of the base material properties in the heat affected zone. Temperature measurements and destructive tests of sample pieces, subjected to different process parameters and conditions, enable identification of minimum and maximum cooling rates, at which the material properties remain in pre-defined limits. Temperature measurements on the surface of the material in proximity to the weld seam enables cooling rate monitoring and material properties evaluation at manufacturing or site installation conditions, when the products could not be subjected to destructive tests. The requirements to the surface temperature measurement during welding and the characteristics of the different measuring devices were investigated for fitness to the purpose of cooling rate calculations, material properties evaluation, as well as for calibration of welding process simulations in volumes in close proximity to the arc welding seams.

Keywords: WELDING, COOLING RATE, TEMPERATURE MEASUREMENT

1. Introduction

The weldability of the metallic materials depends on a range of variables that affect the applicability of the method and the properties of the resulting joints. The properties of the joints of the products could not be assessed in full after the completion of manufacturing process. According to the definition in [1], welding is a "special process" and control before, during and after the completion of the works is necessary, if comprehensive quality requirements are to be met [2].

In arc welding, the increase of the temperature of the material from an initial value (T_0), to values above the liquidus is very fast, and the decrease of the temperature is slower, depending on variable conditions, process properties and material characteristics. Standard recommendations regarding the process variables [3], are issued for the most common materials, like ferritic steels [4], stainless steels [5], aluminium and aluminium alloys [6]. Weldability information is provided by the manufacturers of advanced materials [7].

The calculation of the cooling time in the temperature range from 800 °C to 500 °C, $t_{8/5}$, is possible, according to standard equations, defines by the standards [4], [8]. For example, a three-dimensional heat dissipation the equation was formulated for ferritic steels:

$$t_{8/5} = (6700 - 5 \cdot T_0) \cdot Q \cdot \left(\frac{1}{500 \cdot T_0} - \frac{1}{800 \cdot T_0} \right) \cdot F3 \quad (1)$$

where:

T_0 – initial temperature of the material,

Q – heat input, depending on the arc voltage, current and welding speed

$F3$ – seam geometrical factor.

The equations are valid under limited range of values of the essential variables, considering the thermal properties of all ferritic steels as same. The $t_{8/5}$ is calculated for the cooling of the weld metal.

While the cooling rate in the weld seam is important, the applied ferritic welding filler materials are of chemical composition and mechanical properties, that are superior to the properties of the base materials, irrespective of the cooling time. The area in risk of decreased mechanical properties is situated in the heat affected zone (HAZ), in immediate proximity to the weld seam. For materials of low thermal conductivity the cooling of the weld could not be considered as simultaneous in the whole volume of the weld seam, as the solidification of the liquid metal starts at the sides of the seam and ends at the weld central line, increasing the risk of hot cracking. When the essential variables are outside of the defined limits, the cooling time evaluation is possible on the T/t (Temperature / time) curve, built by measurements performed according to a standard method [4], within the weld seam. The applied method includes

insertion of a thermocouple in the liquid metal after the end of heat application by the welding arc. The method do not provide information for the cooling rate in the HAZ and it is not applicable in manufacturing conditions.

The standard qualification of the welding procedures [9] is based on heat input limitation. It is an indirect method of control of the cooling rate. As shown in (1), the heat input is the only variable influencing the $t_{8/5}$, assuming same thermo-mechanical characteristics of the materials and actual T_0 value, equal to the temperature of the sample pieces during the tests. The above assumptions are not always correct. As reported in [10], the thermal properties of different grade structural steels vary, depending on their chemical composition. Depending on the ambient conditions during the manufacturing process, the T_0 temperature could differ significantly to the initial temperature of the test pieces during the qualification process. Temperature measurement in defined spots in HAZ allows for direct calculation of the cooling rate and more accurate evaluation of the resulting material properties.

Surface temperature measurement in proximity to a welding arc represents a challenge, due to:

- variable thermal conductivity of the different materials surfaces,
- influence of the welding arc emissions,
- high temperature gradient, dependent on the distance to the weld fusion line,
- wide range of temperature values.

The choice of correct sensor properties is of essential importance to the measurement results.

2. Evaluation of surface temperature measurement device parameters

The temperature measurements are possible applying devices of different physical nature. Measuring devices based on changes of the volume/size of a sensitive material, semiconductor and resistive sensors, thermocouples and contactless pyrometers are widely applied in a range of technical applications.

2.1 Thermometers based on volume, length or other geometrical parameter of the sensor

This principle is by far the most widely applied for general temperature measurement, due to the simplicity and reliability of the method. The volume of gas or liquid thermo-sensitive material, or the length of a solid (or combination of solids) material is directly transferred into temperature reading. Disadvantages of the method are the long time necessary for equalization of the temperature in the whole volume of the sensitive material, especially in cases of flat surface temperature measurements. The

method is mostly used for slow processes and when significant surface of the sensitive material volume could be subjected to the temperature.

2.2 Thermistors and semiconductor temperature sensors

The resistor or semiconductor temperature sensors have a limit of temperature measurement which is lower than required for temperature measurements of post welding cooling rates.

For the thermistors, the highest measurable temperatures, depending on the type and model of the sensor, vary up to 300 °C. The semiconductor sensors have even lower maximum measurable temperature, typically up to 120 °C [11]. Semiconductor sensors are frequently employed in combination with thermocouples, in order to provide temperature data at the cold junction, required for compensation during measurements.

2.3 Resistance thermal devices (RTD)

This type of devices use the changes of the electrical resistance of the thermo-sensitive material (Pt 100) with temperature. For some models the measuring temperature range of -40 °C to 1000 °C is reported, as well as measurement spot area of 3.9 x 1.9 mm [13]. While faster than the linear expansion sensors in p. 3.1, the volume of the sensitive material should be heated in order to obtain correct temperature measurement. Electrical current should be supplied by the device to the sensor in order to measure the resistance. The self-heating effect should be considered.

2.4 Thermocouples (TC)

The thermocouples use the Seebeck effect that the junction between a set of two different metals produce emf voltage, depending on the temperature at the place of contact. The thermocouples function on the basis of the difference of the temperature between the cold and the hot end of the materials. The cold junction temperature should be known or measured, for instance by placing the cold end of the materials in ice bath, or in the modern devices, by measuring the cold end temperature by sensor of another type (for instance a semiconductor sensor), and compensating for the difference. The most-common sets of materials form thermocouple types are standardized, designated and their properties are tabulated in [14], together with the equations and constants, required to calculate the generated emf. An excerpt is presented in Table 1. The properties and rules for the compensation cables, used for connection of the thermocouples to the measuring instruments were issued in [18].

Table 1. Properties of high temperature thermocouples, according to IEC 60584-1:2013 (excerpt)

Type	Material (+ leg)/(- leg)	T range, °C, min/max	Tolerance Cl 2, °C @ T max °C	Emf, mV @ 100 / 1000 °C
J	Fe / Cu Ni	-180 to +750	± 5 @ 750	5.269 / 76.373
K	Ni Cr / Ni Al	-180 to +1350	± 9 @ 1200	4.096 / 41.276
N	Ni Cr Si / Ni Si Mg	-270 to +1300	± 9 @ 1200	2.774 / 36.256
E	Ni Cr / Cu Ni	-40 to +900	± 6.75 @ 900	6.319 / 76.373
S	Pt 10%Rh / Pt	-50 to +1750	± 4 @ 1600	0.646 / 10.506
B	Pt 30%Rh / Pt 6%Rh	+100 to +1820	± 4.25 @ 1700	0.646 / 10.506
C	W 5%Re / W 26%Re	+20 to +2300	± 1.0% (T)	1.451 / 18.257

The measuring spot diameter of the exposed thermocouples is in direct relation to the diameter of the wires. The butt welded thermocouples have measurement spot diameter, which is equal to the diameter of the wire. The beaded thermocouples tip diameter

vary up to 2x the wire diameter. The most common thermocouple diameters vary from Ø 0.1 mm to Ø 8 mm. The measuring spot area of bare exposed thermocouples of Ø 0.1 mm could be as low as 0.00785 mm².

According to thermocouple manufacturer's data [16], the maximum measuring temperatures for some types of thermocouples vary, depending on the wire diameter, as in Table 2.

Table 2. Dependence of the maximum measurement temperatures depending on TC wire diameter

Thermocouple type	Type K (NiCr/NiAl)			Type B (Pt 30%Rh / Pt 6%Rh)	
Wire diameter, mm	0.5	1.0	1.5	0.35	0.5
Max T, °C	800	1000	1100	1800	1800

The thermocouples response time depend on:

- The measuring spot contact area / diameter of the probe tip,
- The type of contact of the probe tip to the surface: direct contact of exposed thermocouple wires, grounded or ungrounded insulated thermocouple wires,
- Contact surface medium: air, water, metal to metal etc.

According to [15], the response time in air varies from 1.8 sec for 0.8 mm exposed butt welded wire thermocouples, to above 10 sec for 3.0 mm grounded thermocouples. The response times for beaded and for ungrounded thermocouples are respectively 1.5 x higher.

The response time in water for exposed butt welded wire of 1.25 mm and below is 0.25 sec.

The response time for exposed thermocouples in contact to heated surface, depend on the contact area between the two metals. Bad contact would result in long response times and in measurement errors, due to the thermal resistance of the surfaces.

Recommendation for use of thermocouples were issued by the International electrotechnical commission (IEC) [17], where the preparation procedure, placement, attachment to the measured surface, as well as for the connection to the thermometers were clarified.

2.5 Contactless thermometers

2.5.1 Contactless thermometers based on filament IR emitted radiation

One of the widely used principles of contactless temperature measurement is to pass an IR image of the heated object through an optical system, visible to the operator. The system contains a resistive filament that is heated, depending on the electrical current passing through it, to different temperature. While the filament emitted radiation wave length differs to the measured body radiation wave length, it would be visible to the operator through the optical system. By controlling the current, the wave lengths of the measured object and the filament equalize. The filament shape disappears on the same colour background of the measured object. The measuring temperatures of above 700 °C are common. Disadvantage of the principle is that is not usable for continuous recording of changing temperature values, due to the time necessary for wave length equalization.

2.5.2. Contactless thermometers, based on thermopile sensing elements

The thermopiles are IR detectors, based on multiple micro thermocouple elements, connected in series, in order to obtain highest sensitivity. The IR radiation is focused through an optical system to a matrix of thermopiles, each of these representing one pixel of the thermometer sensor. Commercial thermometers of this type offer a measuring range of -40 to +2250 °C, measuring different wave lengths, from 8-14 µm for the low temperature sensors, to 3.9 µm for the high temperature sensors [19]. The response times of the devices were between 60 and 120 ms. Temperature resolution of 0.1 °C and accuracy ± 1% of the reading are achievable.

The measuring spot size should not be confused with the pointing laser spot, which could be very small in wide range of distances between the sensor and the surface. The actual measuring spot size depends on the optical resolution of the thermo sensors. Optical resolutions Distance: Spot between 10:1 and 120:1 are common. The meaning of optical resolution of 120:1 is that the distance of the sensor to the surface spot is equal to 120 x the measuring spot size. For instance, if the measuring distance is 120 cm, the measuring spot size would be 10 mm.

The optical resolution is generally linear within a range between minimum and maximum distance to the surface. As reported in [20] the minimum measuring spot size for different types of sensors of optical resolution D:S between 9:1 and 75:1 was equal to 6.5 mm.

Challenges to the applicability of the IR contactless thermometers represent:

- The dependence of the measuring accuracy on the measured material surface emissivity in the measured wave length. The shiny metal surfaces are of low emissivity and high reflectivity, which could cause the thermo sensors to read reflected, instead of emitted radiation. In order to decrease the influence of the emissivity double wave length sensors are applied, that measure the difference between the radiation in the two wave bands;
- The proximity of the welding arc during the measurement process influence the contactless temperature measurement sensors. As demonstrated in [21], this challenge was addressed by the application of narrow band filters, passing through the wave lengths that were emitted by the measurement surface, but not the reflected waves and the arc emissions. The complex equipment allowed for high speed measurements at frequency of readings of 60 Hz.

2.6 Fiber-optical measuring systems

The fiber-optical measuring systems have the advantage, that it is not necessary to consider the emissivity of the measured surface, but instead a sensor tip with known emissivity is used. The emitted radiation, or pre-emitted radiation is conveyed via optical fiber. In such way the ambient volume does not influence the measurement in optical or electromagnetic way. The sensor principles include blackbody radiation, luminescence in crystals, refractive index phenomenon in the fiber Bragg gratings or phase shift of two coherent beams in interferometric sensors [24].

The surface measurement spot size of the fiber-optical system in the range from 0.4 mm, to volumetric systems utilizing fiber sensors with lengths of hundreds of meters were reported [25].

The response times of the blackbody radiation systems in the range between 1 ms to 100 ms, and the temperature range of 800 to 2000°C was reported [26], utilizing single crystal sapphire fiber sensor.

A disadvantage of the fiber optical systems include great complexity of the equipment, if wide temperature range is to be achieved, including combinations of several types of sensors in the different areas of the range.

3. Requirements for temperature measurement for cooling rate monitoring and calculations in HAZ during and immediately after welding

Depending on the requirements for the measurements, there are several evaluation criteria for the devices and sensors, in order to be applicable for surface temperature measurements and real time monitoring, in close proximity of the weld seam, in manufacturing conditions or at site installations.

3.1 Temperature range

The required temperature range varies, depending on the material, which would be subject to temperature measurement. The lower limit of the range could be set based on evaluation of the lowest initial material temperature. The higher limit of the range depends on the material liquidus, thermal conductivity and phase transformation temperatures.

For ferritic structural steels, operational temperatures down to -60 °C are possible and site ambient temperatures could be considered

as equal to the initial material temperature. A phase transformation from ferrite to austenite occurs at temperature range between 700 and 900 °C. The phase transformation temperature for same type of steel increases in cases of high speed heating, similar to the conditions occurring during arc welding processes [21]. The sensors range for high temperatures measurement between the liquidus and the transformation temperature could be considered as appropriate.

3.2 Accuracy and tolerance limits of the measurements

The permissible tolerance limits of the measurements time-temperature cycles related to heat application to welded structures is set in table 44 of [22], as ± 10 °C. In [23] the tolerances of the heat treatment measuring equipment were set as in [14].

3.3 Latency

The latency of the measuring system should be appropriate for the required measurement accuracy. Initial data for the cooling time for ferritic structural steel was obtained by a measurement, performed with K-type thermocouples, positioned at 3 mm from the weld seam edge, and at 11 mm from the weld seam line. The maximum temperature measured a 3 mm from the weld seam edge was 1040 °C, and at 11 mm was 480 °C, Fig. 1a.

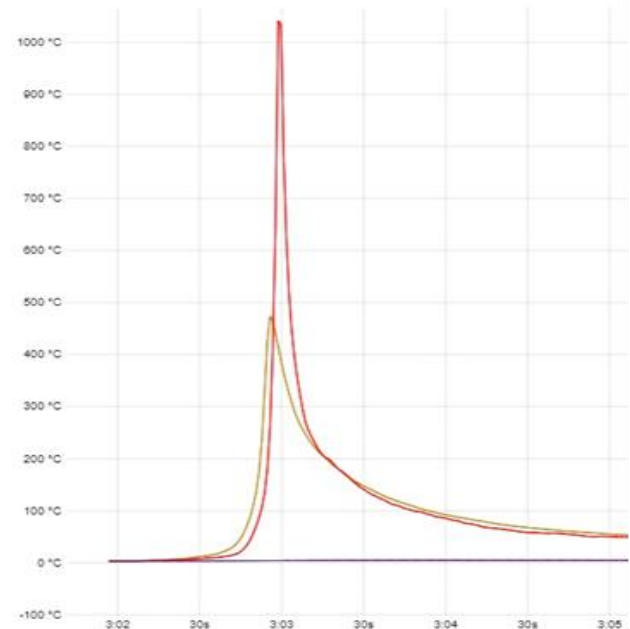


Fig. 1a. Temperature cycle measured at welding heat affected zone at distance of 3.2 mm and at distance of 11 mm from the weld seam edge, with maximum temperatures of 1040 °C and 480 °C respectively.

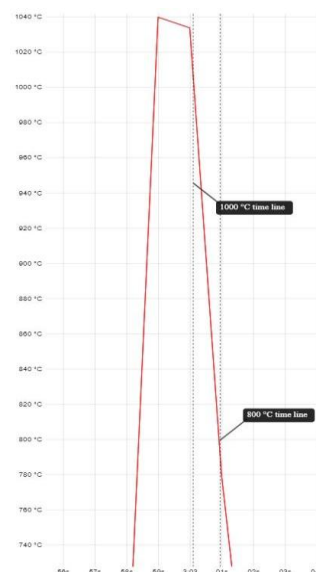


Fig. 1b. Measuring of the time of temperature decrease from 1000 °C to 800 °C

The decrease of the temperature of the material at 3 mm from the seam line, 1000 °C to 800 °C was calculated on the graphic,

by deducting the time when temperature decreased to 1000 °C to the later time, when the temperature decreased to 800 °C, to be 1 second, as on Fig. 1b.

In order to achieve measurement accuracy of ± 10 °C, in case that the temperature decrease from 1000 °C to 800 °C happened in 1 sec, measurements frequency of above 20 readings per second would be necessary. In order to fulfill the requirements of the sampling theorem, in order to read at frequency of 20 Hz, at least 2X sampling rate of the sensors would be required. In such case frequency of 40 readings of the sensor data per second would be adequate.

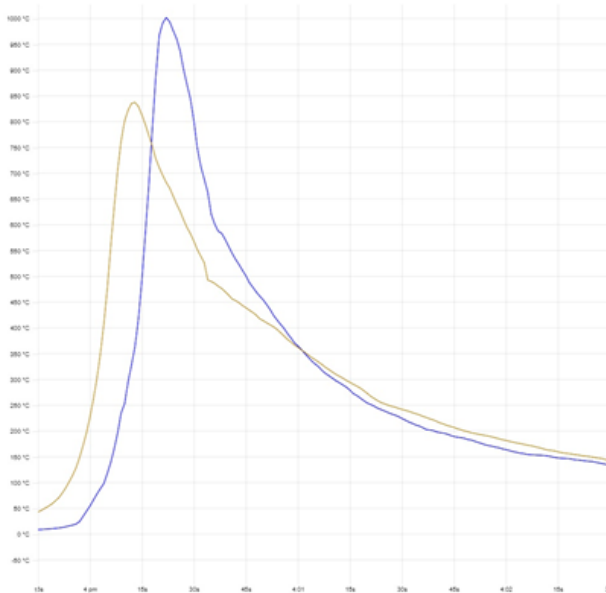


Fig. 2 Temperature cycle measured at welding heat affected zone at distance of 3.5 mm and at distance of 5.5 mm from the weld seam edge, with maximum temperatures 1000 °C and 840 °C

3.4 Measurement spot area

The measurement of the temperature occurs on area of the object surface, specific to the type of sensor that is used. The different sensors have different area resolution, depending on the size of the sensor, distance to the measuring spot, disturbances caused by movements or vibrations etc. Measuring the temperature near the seam edge require smaller measurement spot diameter, compared to measurements at a bigger distance. According Fig. 2, obtained for structural ferritic steel, the maximum temperature measured at 3.5 mm distance to the seam edge was 1000 °C. At 5.5 mm distance from the seam edge, the maximum measurement temperature was 836 °C. Comparing the data obtained during the measurements presented on Fig. 1 and Fig. 2, the temperature difference per mm distance to the weld seam edge could be calculated, as in Table 3. The targeted measurement accuracy of 10 °C at distance to the weld seam with maximum temperature of 1000 °C could be achieved by determination of the measurement position with resolution of 1 / (133/10), mm / (°C / mm). The measurement principle and the sensor spot size should allow determination of the measurement position within a tolerance of the distance to the weld seam edge of ± 0.075 mm. In cases of measuring spot diameter, bigger than the requirement for the accuracy of the distance to the weld seam edge, the equalisation of the temperature in the sensor could be calculated. For the non-contact sensor types, the temperature equalisation could be evaluated by calculation of the filters wave length band, to pass the radiation emitted from a spot corresponding to 3 pixels of the sensor matrix. For contact sensors, RTD, TC, the temperature equalisation at the sensor element could be calculated, based on the volume of the sensitive material and the difference between the thermal conductivity of the measured material and of the sensor material. In general, the cases when the thermal conductivity of the sensor material is higher than the thermal conductivity of the measured material are preferential.

Table 3. Maximum measured temperature measured at different distances from the seam edge

Measurement Nr.	1a	2a	2b	1b
Distance, mm	3.2	3.5	5.5	11
Max temperature, °C	1040	1000	840	480
Distance (n+1) – (n), mm	0.3	2.0	5.5	
ΔT , °C	40	160	360	
ΔT to distance, °C / mm	133	80	65	

3.5 Other evaluation criteria

Influence of the external disturbances to the accuracy of the measurements. The welding arc emissions in wide spectre of radiated heat, waives and rays require comprehensive evaluation of measures for disturbance avoidance. The presence of fumes and smoke of varying consistency during the process represents an obstacle to the non-contact measurements.

The applicability of the measuring devices depend on additional factors, which influence the possibility to implement these in manufacturing or site installation conditions, including complexity of the equipment [12] and economic considerations.

4. Results

The properties of the temperature measuring devices were evaluated as of high, medium, low or insufficient compliance to the above defined requirements, as in Table 4.

Table 4. Evaluation of the correspondence of the thermometer properties to the requirements to surface temperature measurement in the HAZ during and after welding

Sensor type / criteria	Temp. range, °C	Accuracy °C	Latency, readings	Measuring spot	Other criteria
Required range	-40 to 1000 °C	± 10 °C	< 40 per sec	$\varnothing 0.1$, mm	reliability
Volumetric	High	High	Insufficient	Low	High
Semiconductor	Inadequate	High	High	Low	Medium
RTD (Pt100)	High	High	Low	Low	High
TC K, □□□	Medium	Medium	Medium	Medium	High
TC K, □□□	High	Medium	Medium	Low	High
TC N, □□□	Medium	Medium	Medium	Medium	High
TC N, □□□	High	Medium	Medium	Low	High
TC, type B	High	High	Medium	Medium	High
IR / filament	Medium	High	Insufficient	Insufficient	Low
IR / thermopile	High	High	High	Insufficient	Low
Fiber-optical	High	High	High	Medium	Low

5. Conclusions

The welding of materials with advanced-properties, achieved by precise control of the chemical composition, combined heat and mechanical treatment during manufacturing, could require additional control of the temperature cycles during the process. Direct measurement of the temperature in the weld seam is possible by non-contact optical devices, in cases when the measurement spot of considerable size is acceptable. The accuracy is subject to various

influences. The validity and accuracy of the results is influenced by a number of factors, including material surface emissivity, reflections, weld arc emissions, humidity, and intermediate air/medium quality among others. The utilization of non-contact optical measuring devices is limited to manufacturing with high repeatability, performed in controlled conditions, and by the complexity of the measuring equipment if reliable results are to be achieved.

Direct measurement of the temperature is possible in close proximity to the weld seam, utilizing single or multiple spot thermocouple sensors, allowing monitoring of the welding thermal cycles, as well as additional process parameters including welding speed (arc travel time between sensors positioned along the seam), actual preheating temperature at process start, inter-pass temperatures, heat treatment parameters etc.

Calibration of welding process simulation models is possible by multi-spot temperature measurement, allowing evaluation, in a finite number of positions in the whole volume of the heat affected material, of different properties of the metal materials, including convection and radiation at the surface; stress versus strain; materials changes such as microstructure transformations; dissolution or precipitation; mechanical behaviour such as elasticity; instantaneous or time dependent-plasticity; strain hardening and recovery effect; thermal expansion; transformation induced plasticity [11].

Measuring of the temperature on the surface, at pre-defined distance from the weld seam line, could be utilised for an evaluation of the material properties in the welding manufacturing process. Determining of t_{8/5} limits in defined spots or distances to the weld seam edge during procedure tests and continuous monitoring of the temperature during manufacturing allows for increased control of the joint properties. The type and the design of the thermocouple sensors should be carefully reviewed due to the operation close to the properties and applicability limits of the measuring devices. Multiple sensors of different type could be applied in order to address the limitations.

6. References

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Effect of technological parameters on the structure and physico-mechanical properties of Fe-FeCr800 system composite

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Abstract: The article investigates the influence of technology and technological parameters of manufacturing on the structure, phase composition and physical and mechanical properties of the Fe-FeCr800 composite system. It was established that the determining factor in phase and structure formation is the manufacturing technology, while vacuum sintering and hot forging have their own optimal technological parameters. At the same time, hot forging makes it possible to obtain a composite with a higher microhardness of structural components due to a change in the content of component components. The results of the research also showed that the preheating time (for 20 min.), as well as thermomechanical treatment, is sufficient for the phase formation process, in particular, with the release of carboboride phases of the type Me_3CB and $Me_3(CB)_2$, while the densification processes are intensified, which makes it possible to obtain a material with lower residual porosity. Analysis of mechanical tests showed that vacuum sintering makes it possible to obtain composites with higher mechanical properties due to the active interaction between the components of the composite. However, high-temperature annealing after hot deformation will allow for a composite with high mechanical properties.

KEYWORDS: COMPOSITE MATERIALS, CARBIDE STEEL, HOT FORGING, MECHANICAL PROPERTIES, HARDNESS.

Introduction

Ensuring an increase in the service life of components and mechanisms requires the development of materials with the highest possible physical, mechanical and functional properties. At the moment, carbide steels have shown their effectiveness and are used in components and mechanisms to increase the service life of the unit as a whole. Every year, the search for new component compositions of the material becomes more and more difficult, since a lot of research has already been conducted on the development of carbide steels with different component compositions. However, all attention was paid to classical methods of powder metallurgy. Therefore, changing the technology for obtaining a composite will make it possible to obtain a material with other functional properties. Over the years of research, powder metallurgy methods have already proven their superiority over foundry production, since sintered materials are distinguished by a homogeneous structure and low anisotropy of properties. However, classical technological schemes of powder metallurgy, which include the preparation of the charge, pressing and sintering of the material, do not always ensure the production of an absolutely compact material (especially with a high content of carbide components). As a result, there is a need to apply technological schemes based on the use of high-temperature sintering in the presence of a liquid phase, static hot pressing or electric discharge sintering, etc. [1–4].

To obtain and compact the material, hot forging of porous blanks has proven to be one of the most effective processes of powder metallurgy [5–9]. Hot plastic deformation allows obtaining semi-finished products of both simple and complex shapes with low residual porosity, low defectivity and high material uniformity. In addition to high density, hot plastic deformation allows obtaining a fine-grained structure without additional heat treatment [10].

A significant number of publications are devoted to the production of chromium carbide steels by powder metallurgy methods [11–14], but there are almost no publications on their production by methods of metal processing by pressure (including hot forging). Therefore, the purpose of this work is to establish the influence of the production technology and technological parameters of production on the structure, phase composition and physical and mechanical properties of the composite of the Fe-FeCr800 system.

Experimental Procedures

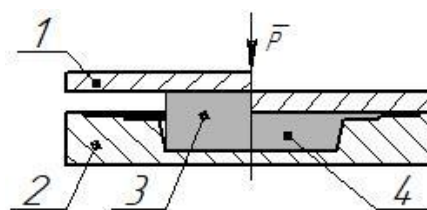
The starting materials were iron powder, high-carbon ferrochrome FCr800 and titanium boride powder. Titanium boride was introduced with the aim of forming, during subsequent thermal synthesis, the corresponding boride phases, which increase the hardness and wear resistance of the resulting materials [15]. High-carbon ferrochrome powder was obtained from lump FKh800 by mechanical grinding in a jaw crusher and subsequent grinding in a

ball mill. Powder mixtures were prepared by wet grinding-mixing in a ball mill in an alcohol environment, the composition of the mixtures is given in Table 1.

Table 1 - Content of components of the initial mixtures of Fe-FeCr800-TiB₂ composites

№	Element content, (% wt.)		
	Fe	FeCr800	TiB ₂
1	65	35	-
2	64,25	35	0,74
3	63,5	35	1,5

The samples were formed in a closed mold at a pressure of 800 MPa. Then the samples were prepared at home by different methods: 1 - sintering in a vacuum electric furnace at 1100–1250 °C with an isothermal holding time of 30 min.; 2 - hot forging in an open die, the scheme of which is presented in Figure 1, for hot deformation the workpieces were preheated in a furnace in an argon environment to a temperature of 1100°C and 1200°C for 20 min..



1- striker; 2 - matrix; 3 - blank; 4 - forging;
Figure 1 - Hot forming deformation

The hardness of AMKM was determined using the Novotest TC-BRV device using the Rockwell method (HRA scale). Bending tests of samples were performed using a three-point scheme on the RM 102M machine using a special device, on the plate of which two prisms were installed, on which a sample measuring 6x6x42 mm was placed. The load on the sample was applied in the middle of its length. The distance between the prisms was 35 mm. The microhardness of the structural components was determined using the PMT-3 device at room temperature and a load of 10 - 25 g, the holding time was 30 s. The measurement area should be twice the diagonal of the impressions. The measurements were repeated 10 - 30 times. The microstructure and phase composition of the sintered samples were studied using optical ("Olympus LX-70") and electron (TESCAN VEGA) microscopes. Local micro-X-ray spectral analysis was performed on an MS-46 microprobe.

Results and discussion

To establish the optimal conditions for obtaining chromium carbide steels, studies were conducted on the influence of sintering temperature and technological parameters of hot forging on the structure and properties of composites. From the given Fig. 2 it is

seen that samples of different compositions obtained by hot forging have significantly higher density values than composites obtained by sintering in vacuum. This dependence can be explained by the fact that in the process of hot forging, densification processes are intensified due to the application of additional mechanical energy.

During sintering in vacuum at temperatures of 1100-1150 °C, shrinkage occurs mainly as a result of solid-phase sintering, but the alloys do not reach high shrinkage and density, which is explained by the low intensity of diffusion processes, as well as the inhibition of the compaction of the samples by the carbide framework. Therefore, to intensify the compaction process of the samples, it is necessary to increase the sintering temperature to 1200-1250 °C, since as a result of reaching the eutectic temperature, a liquid phase appears and the mechanism of liquid-phase sintering is implemented.

Thus, the hot forging method without the use of a vacuum environment allows you to obtain high-density composites compared to sintered samples. Accordingly, the density of the forged materials is slightly higher 6.92÷7.32 g/cm³ compared to the sintered 6.44÷7.1 g/cm³.

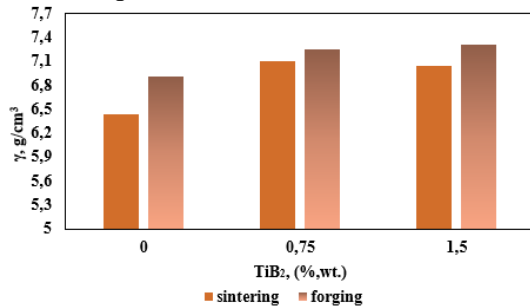
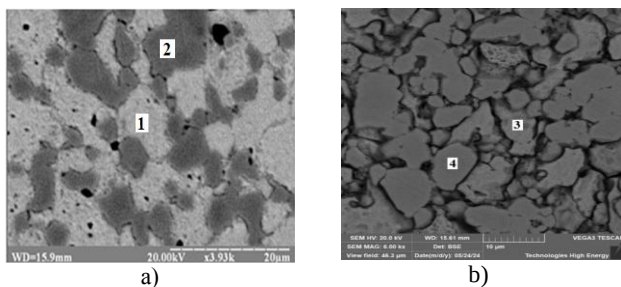


Fig. 2 – Effect of titanium boride content on the density of sintered and hot forged composites at optimal temperatures

The microstructure was investigated and the distribution of elements in the metal and carbide phases of composites obtained by different methods was determined. Fig. 3 shows the microstructures of the sintered (Fig. 3, a) in a vacuum environment at 1250 °C and forged at a preheating temperature of 1200 °C with a holding time of 20 min. (Fig. 3, b) composite of the composition Fe-35FeCr800. Regardless of the method of obtaining the composite, their structure remains heterogeneous, consisting of an elastic-plastic matrix and carbide phases located in it. Namely, the light gray phase is an iron-chromium matrix of the composite, the gray phase is a complex iron-chromium carbide of the M₃C type.



Spec trum No.	Fe	Cr	C	Carbide phase type	Microhardnes s, GPa
1	82,75	17,07	0,17		2,8
2	19,78	73,13	7,08	M ₃ C	10,5
3	88,84	10,93	0,22		3,69
4	22,26	68,79	8,94	Me ₇ C ₃	11,5

Fig. 3 – SEM and EDS analysis of the Fe-35FeCr800 composite (% wt.) obtained by vacuum sintering (a) and hot forging (b)

According to the analysis conducted using an energy dispersive microanalyzer, the elemental composition of the matrix and carbide phases of the forged composite differs from the sample

obtained by vacuum sintering (Fig. 3). From the table in Fig. 3, after hot forging, the microhardness of the phases in the Fe-35(%, wt.) FeCr800 composite slightly increased, the matrix phase - 2.8÷3.69 GPa and the carbide phase - 10.5÷11.5 GPa, which is associated with the redistribution of chemical elements in the structural components of the studied material.

Fig. 4 shows the microstructure of hot forging composites of different compositions obtained at different preheating temperatures. The structure, chemical composition and size of the phases are affected by the content of titanium boride in the charge and the temperature of hot deformation. As can be seen from Fig. 4, at a titanium boride content of 0.75 (% wt.) the composite structure consists of a matrix, iron-chromium carbide (light gray phase) and carboboride (dark gray phase). At the specified titanium boride content, the formation of the 3rd phase (dark gray) of the Me₃CB carboboride is clearly recorded already at a heating temperature of 1100°C. Increasing the preheating temperature to 1200°C for this composition leads to grain growth. The composite doped with 0.75 (% wt.) of titanium boride obtained at a preheating temperature of 1100°C differs from other hot-forged composites in its fine-grained structure (Fig. 4, d). The composite with a composition of 1.5 (% wt.) of titanium boride after thermomechanical treatment has a coarser-grained structure, especially at a temperature of 1200°C (Fig. 4, c). The structure of this sample consists of a matrix, iron-chromium carbide and carboboride. However, the amount of carboboride in the structure of this composite varies significantly depending on the heating temperature. At 1100°C, carboboride of the type Me₃(CB)₂ is fixed on the structure in a small amount (Fig. 4, b), but with an increase in the heating temperature to 1200°C, its amount increases and is clearly displayed in the form of dark gray phases (Fig. 4, c).

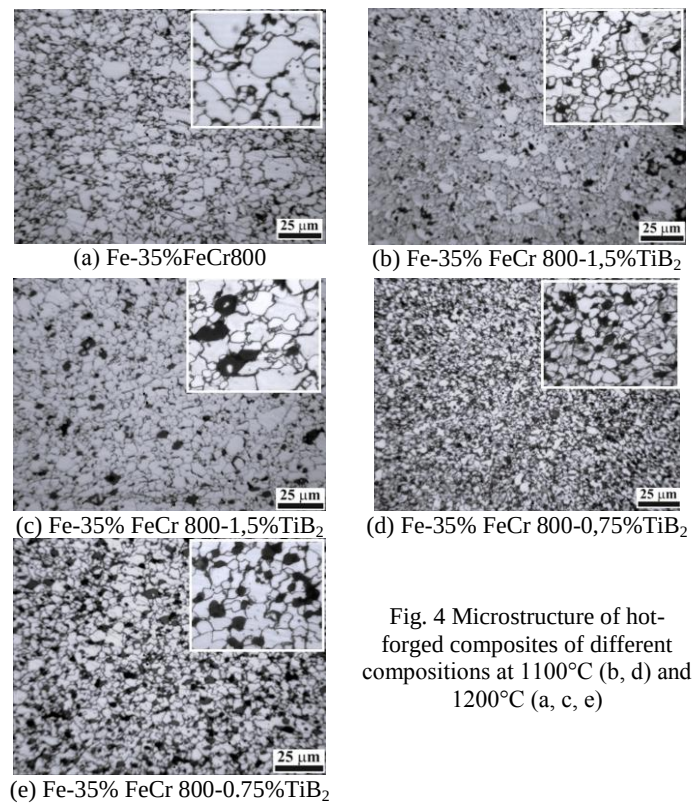


Fig. 4 Microstructure of hot-forged composites of different compositions at 1100°C (b, d) and 1200°C (a, c, e)

One of the advantages of hot forging samples, in contrast to those obtained in the process of vacuum sintering, is that after deformation of samples doped with titanium boride, the grains of the matrix and carbide components of the composites were crushed. Also, hot forging allows you to obtain a composite of higher density even without prior sintering in a vacuum. Also, the preheating time (for 20 min.) before deformation and the applied energy during impact loading are sufficient to undergo the phase formation

process, in particular, with the separation of carboboride phases such as Me_3CB and $\text{Me}_3(\text{CB})_2$.

Metallographic studies and local micro-X-ray spectral analyses of materials based on the Fe-35% (wt.) FeCr800 – TiB_2 system obtained by hot forging showed (Fig. 5) that a microheterogeneous, multiphase microstructure of the composite is formed, containing complex iron-chromium carbides Me_3C_2 and complex carboborides of the $\text{Me}_3(\text{CB})_2$ type.

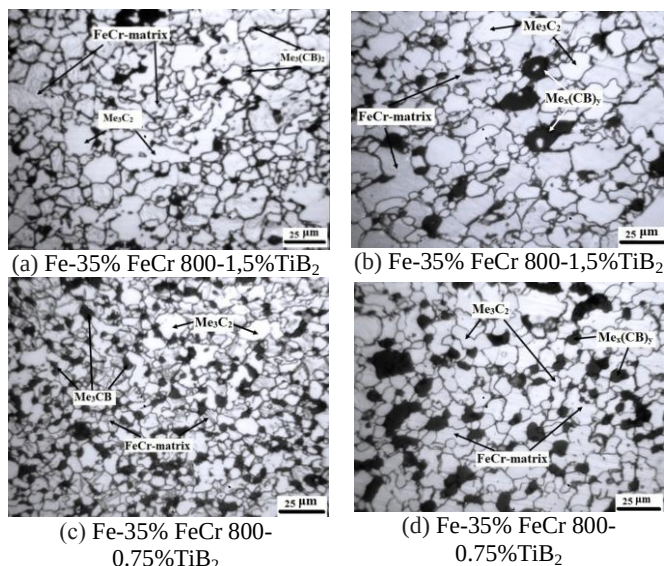
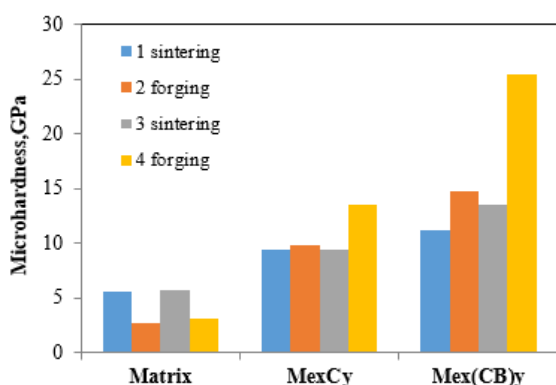


Fig. 5 Micro-X-ray spectral analysis of titanium boride-doped composites at different preheating temperatures of 1100°C (a, b) and 1200°C (c, d)

The microhardness of the structural components of the composites doped with titanium boride was also investigated (Fig. 6). As can be seen from the graph, the microhardness values of the carboboride phases of hot-forged and vacuum-sintered composites are significantly different. For example, in the composite of the composition Fe-35%FeCr800-0.75%TiB₂ obtained at a sintering temperature of 1250 °C, the microhardness value of the carboboride phase is 12.5 GPa, and in the composite Fe-35%FeCr800-1.5 %TiB₂ - 13.7 GPa, which is significantly less than in the hot-forging samples. This is explained by the difference in the elemental composition of the corresponding phase of the hot-forging and sintered composites.



1,2 - Fe-35%FeCr800-0.75%TiB₂; 3,4 - Fe-35%FeCr800-1.5%TiB₂
Fig. 6. Microhardness of structural components of the composites

The hardness of sintered and hot forging samples is shown in Fig. 7. A comparison of the obtained values showed that hot forging allows obtaining composites with almost the same hardness as sintered ones. However, the hot-forged composite of the composition Fe-35%FeCr800-0.75%TiB₂ stands out, the hardness of which increased from 75 to 79 HRA compared to the sintered

one, which is associated with the crushing of the structure after hot deformation.

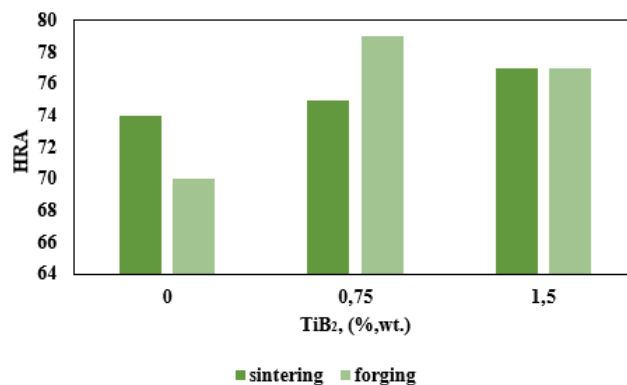


Fig. 7. Hardness of composites obtained with optimal technological parameters

Conclusions

1 The influence of the technology for obtaining carbidosteel of the Fe-FeCr800 system on its structure and properties was studied. It was shown that sintering in vacuum and hot forging allows to obtain composites with a homogeneous structure. At the same time, hot forging provides a finer microstructure compared to sintered composites, as well as a different ratio of components in the structural components, which leads to an increase in their microhardness.

2. The results of microstructural analysis showed that the preheating time (for 20 min.) before deformation and the applied energy during impact loading are sufficient to undergo the phase formation process, in particular, with the separation of carboboride phases of the type Me_3CB and $\text{Me}_3(\text{CB})_2$.

3. The results of the studies showed that the hardness of hot forging composites was relatively unchanged compared to sintered ones.

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