10th Youth Symposium on Experimental Solid Mechanics

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PREFACE

The Youth Symposium on Experimental Solid Mechanics was established under the auspices of the Danubia Adria Society on Experimental Methods and the IMECO Technical Committee 15, President Prof. Lajos Borbas.

In the year 2002 Prof. Alessandro Freddi organized the first edition of this Symposium in Bertinoro-Forlì at the Bologna University Center in Italy. Thereby was founded the first international forum for young researchers and engineers, PhD students and students, working in the field of experimental solid mechanics.

It is a great pleasure that the 10th anniversary of the Symposium takes place on the Chemnitz University of Technology in Germany and can be organized by the Chair of Solid Mechanics.

I am pleased to present the proceedings of the 10th Youth Symposium. It includes about 50 extended abstract from participants of

Austria, Brazil, Croatia, Czech Republic, Germany, Hungary, Iraq, Italy, Poland, Romania, Serbia, and United Kingdom.

My thanks go to the complete local organization Committee especially to Mr. Jens Kretzschmar for developing with motivation and enthusiasm the internet presence including the complete program and the proceedings. I would like to thank Mrs. Ines Voigt for organizing the conference venue the hotel reservations and the technical visit as well Mrs. Annelie Thiele and Mrs. Eugenie Tereschenko for her help in various ways.

Finally I should express my gratitude to:

- All participants, who influenced the quality of the Symposium with the scientific contributions, the lectures, posters and discussions
- The scientific committee for giving Chemnitz the task to organize the anniversary of the Youth-Symposium
- The donators for their support even in this complicated economic situation.

PD Dr. Martin Stockmann
Chemnitz, May 2011
Chairman of the local Organizing Committee
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THE HISTORY AND DEVELOPMENT OF THE YSESM FROM 2002 TO 2011

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Abstract

This article reviews the origin and development of the Youth Symposium of Experimental Solid Mechanics (YSESM), since the first germinal idea to its tenth 2011 edition in Germany.

Introduction and origin

When I became President of the Technical Committee of IMEKO International Measurements Confederation after prof. Laermann completed his presidency, I was interested to explore any form of development in the committee’s activities. In spite of the great attention and care of my predecessor, TC15 had to compete with many other events on Experimental Mechanics around the world. After exchanging views about the possibility to regionalize the role of TC15 with prof. Laermann, who had become Honorary President of TC15, I organized a TC15 meeting during the “XXV AIAS International Conference on Material Engineering” in Gallipoli-Lecce, (Italy) in Sept. 4-7, 1996. It was then a sensible result to gather together 12 representative members of TC15 and, one year later, on the occasion of the “XIV IMEKO World Conference” in Tampere, Finland, in June 2-6 1997, the Technical Committee organized a special session on Experimental Mechanics with a remarkable plenary lecture by Dr. Akira Umeda (Osaka, Japan).

Finally, during a break at the ICEM Conference in Oxford in 1998, some colleagues, convened in a place to relax and discuss whilst refreshing with ale, showed significant interest in the idea of organizing conferences at very low cost, in a Centre created by Bologna University to be devoted to Summer Courses and other cultural activities in Bertinoro, a medieval castle along the Roman “Via Emilia”, (close to the Adriatic Sea), (http://www.centrocongressibertinoro.it/).

The problem was and still is deciding what to do in the field of Experimental Mechanics without conflicting with other initiatives which, in Europe only, are numerous, such as, ICEM or Danubia-Adria and a large number of national meetings in the same field. On the contrary, the scope of TC15 should be to collaborate in a joint effort to create a network for the exchange of informal ideas, especially in the interest of the new generations.

Some time was necessary to implement this idea, since it was essential to explore the feasibility of this initiative, discussing it with several colleagues, authorities and friends. Dominating the scene were the relations between these international societies and also the changing character of some of these institutions: ICEM was going to generate the EURASEM Society, Danubia-Adria was considerably extending its influence in Central Europe, and IMEKO was considering reviewing its relationship with YSESM, in the interest of a greater participation of young researchers. This point was in fact extensively discussed and a solution found with the IMEKO Secretary some years later during the YSESM in Castrocaro Terme.

The first conference in the YSESM series took place in Bertinoro, in the early Spring 2002. As an outcome of this conference, an organizing
committee was formed with the enthusiastic help of many young PhD students and young researchers, whom I wish to remember in this primitive digital picture.

Among others, first from left is PhD C. Fragassa, who was the chief Executive of the Organizing Committee, who would continue to organize, stimulate and promote the consecutive editions of the Symposium for almost six years. (He was also the webmaster of the websites of these editions). Next is Prof. Francesca Cosmi, Professor at Trieste Engineering Faculty, currently a Regular Member of the Scientific Committee of Danubia-Adria Society (as Italian Representative on behalf of the Italian Society of Stress Analysis). On the right-hand side of the picture are PhD P. Morelli and PhD A. Zucchelli, both Assistant Professors at Bologna University Faculty of Engineering.

The new scientific society took on naturally the modus operandi of the Danubia-Adria Society that was and is the main sponsor of YSESM in helping to organize the annual meeting, and in sending young graduates, PhDs and Post Docs. PhD G. Minak, Assistant Professor at Bologna University, is not in the picture, but was and is an active members. He will be the future Italian Representative in YSESM (again on behalf of the Italian Society of Stress Analysis).

Organisation

The organizational model of YSESM was empirically adapted to the specific needs by the young spirits of its members, but special attention was paid to the Danubia-Adria experience, which represents a mature example of a mixture of good tradition and a more informal spirit of experimentation. The institutional habit can be seen in the fact that the Representatives of the different Countries are nominated by the National Societies of Experimental Mechanics and a special link with IMEKO TC15 is defined in a protocol presented to the IMEKO Secretary, (at the time, Prof. Tomas Kemeny, who personally attended the third Symposium in Castrocaro Terme, Italy). The experimental attitude will probably bring the society to a more formalized structure, with the planning of a statute.

However, the main feature of the current structure, in the same way as Danubia-Adria, is its fully democratic organisation, since every year the Organizing Committee changes and takes full responsibility for the Symposium, from the financial to the scientific and organisational aspects, and equality among members is obtained through the rotation of the tasks. It may well be that not all members have the same opinion of the method, but the mother society Danubia-Adria demonstrates the success of this idea in about thirty years of activity.

In principle, the society is open to countries all over the world, and, from the beginning, a large number of active members came also from very distant parts of the globe, especially from Latin America (Argentina and Brazil) and also from Georgia, and they generally continue to be present at the annual meeting.

Problems exist in extending the knowledge of this society for young people to other countries (even if the countries represented are now reaching the remarkable number of twelve). Some efforts, as my talk and invitation to the 13th ICEM Conference Committee in Alexandroupolis, were devoted to find other interested countries (like, for example, United Kingdom, France, etc.).

A very important extension is this year’s organisation done by Germany. The entering of a nation of this weight is a great event that will probably have a positive effect on the Danubia-Adria Symposium. It is not the first time that YSESM favoured opening the doors to other National Societies for joining the mother society. The same happened with Serbia, which, after organizing the YSESM, decided to enter into the Danubia-Adria.

Rationale

It is worth examining the mission of the largest Experimental Mechanics Society in the world, SEM United States, to understand the development of this scientific discipline and human society in the last fifty years:

"The Society for Experimental Mechanics, [was] originally called The Society for Experimental Stress Analysis, .... with the original goal to "further the knowledge of stress and strain analysis and related technologies."

In the years since its founding, SEM has continued to adapt itself to the needs of the members in the experimental mechanics
community. …This international network of engineers and scientists is interested in the research and application of engineering measurements and test methods with the mission to promote and encourage the furtherance of knowledge pertaining to the education, research and application of experimental mechanics to the determination of materials and system behaviour…” (see http://www.sem.org/)

These points can be wholly subscribed to, as well as the attractive revisiting of the old name Experimental Stress Analysis in the title of the Proceedings of the 13th ICEM, as Experimental Analysis of Nano and Engineering Materials and Structures, which launches the new nano-materials perspective.

In the writer’s opinion, therefore, the new approach of YSESM does not consist in a new mission, but in the commitment of the young branch of researchers to organize occasions for comparing experiences, proposing joint researches and exploring new or unusual ways of scientific collaboration and, last but not least, convivial life. The main focus of this collaboration is not experimenting in itself but experimenting for design as a meeting point for widely differing research disciplines. YSESM wishes to facilitate the establishment of new research programmes, to provide input to policy makers, and to identify suitable funding sources for research in mechanical design. YSESM can survive only if it provides both a scientific and organisational framework for this research.

**Symposia series**

The dates and venues of the symposia which followed the Bertinoro First Symposium are given in the following table, together with the websites containing information about the programmes and, (although not always), the titles of the presentations. After the first edition, the proceedings of the extended summaries were printed with ISBN reference numbers.

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<tr>
<th>Date</th>
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<td>2002 6-9 March</td>
<td>Bertinoro, Italy</td>
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<td>2011 25-28 May</td>
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THE PAST, THE PRESENT AND THE FUTURE OF POLICAB: 
THE CHALLENGE OF SYNTHETIC MOORING ROPES 
ANCHORAGES AT PRE-SALT PETROLEUM BASIN, IN BRAZIL 

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1. Introduction

The technology of platform anchorage systems has been developing quickly nowadays. Due to the oil extraction in deep waters, the traditional mooring types, such as anchor cable/wire rope/anchor cable in catenary shape, were replaced by a “Taut-Leg” geometry. This type, characterized by an anchor cable/synthetic cable/anchor cable, operates in a straight mode, reducing weight and modifying the mooring operation. Therefore, the synthetic ropes for offshore platform anchorage are long and robust, with large number of sub-ropes arranged in parallel mode in order to increase mechanical strength. Petrobras has used this new form of technology with polyester cables since the beginning of the last decade; despite this fact, there is a lack of knowledge of the behavior related to structural integrity (residual life).

Fig. 1: Offshore anchorage: catenary x taut-leg.

In 2001, with the support of PETROBRAS, the laboratory was created and started studying and researching about synthetic ropes for offshore mooring application. Our study started with yarns (polyester multifilaments) and now we begin to learn about ropes architecture as well as rope and sub rope tests.

POLICAB is a laboratory focused on the study of stress, strain and strength regarding synthetic ropes. POLICAB is part of the Engineering School at Federal University of Rio Grande.

2. The structure of POLICAB

a) Research room with computational facilities (internet, library and individual student desks) for mechanical engineering undergraduate students.
b) Photomechanical lab with Banch and reflection polariscopes, kits of tools, devices and facilities.
c) Tests and Assembly Saloon. Reaction structure for cable and rope strenght tests.
d) Climatized room for mechanical tests with:
   • Servo-hydraulic machine (Instron), 100kN (push, pull and fatigue);
   • Electro-mechanic test machine, 20kN (traction, compression and creep);
   • Facilities and accessories for handling and testing synthetic ropes: hydraulic and pneumatic Clamps, special clamps for ropes and cables.
e) Meeting and educational (teaching) room, with projection facilities (data show, computer) and specific library.
f) Computational room for researchers, graduated and undergraduate students, where they develop researches related to the laboratory.

3. The Research at POLICAB

Materials Research: Development of new products and their mechanical behaviors. HMPE (High Modulus Polyethylene); Polyester; Other synthetic materials (polyblend, polysteel, ...).

Rope Architecture: We intend to know the behavior of devices and some working situations:
   – Rope terminals
   – Fatigue (scale ropes)
   – Creep
   – Rope storage
   – Wearing (COATS)

Mechanical Modelling:
   – Theoretical:
     • Damage and structural integrity
     • Creep
     • Tenacity
Experimental:
- Strength: Scale factors
- Creep
- Residual life
- Yarn on yarn abrasion.

3.1 Rope jackets abrasion.

The endurance of the rope jacket of a mooring rope is very significant to verify the real life of the rope. If the jacket were broken or discontinued by wearing, we consider the rope collapsed. Figure 2 shows a sketch of the wearing and abrasion device.

Fig. 2: Abrasion under different kinds of tension situations

The graphic 1 shows the behavior of a synthetic rope jacket (HMPE) when submitted for two kinds of test: tension plus bending and tension in tap water.

Graphic 1: HMPE: tension + bend and tension submerged in tap water

3.2 Mechanism of yarn-on-yarn abrasion

POLICAB tests the abrasion behavior of the multi-filaments, according to the Cordage Institute and ASTM rules. In figure 4 we show a sketch and in figure 5 we can see the prototype.

Fig. 4: Yarn-on-yarn device.

Fig. 5: Yarn-on-yarn prototype.

The graphic 2 shows us the behavior of polyester multifilament when submitted to the upper abrasion test.

Graphic 2: Abrasion test result

3.3 Creep Research – HMPE.

The focus of the research: anchoring the offshore structures in ultra high deep waters. The production of know-how regarding the mechanical behavior – creep – for synthetic materials, as Polyester and HMPE (High Modulus Polyethylene). This permits the use of those materials for the production of synthetic mooring ropes. Today, we develop theoretical and experimental studies regarding creep.

Theoretical Research: Modeling the structural integrity for materials with visco-elastic behavior, using CDM (Continuous Mechanic Damage) with the LMTA – Laboratório de Mecânica Teórica e Aplicada, UFF – Universidade Federal Fluminense, Rio de Janeiro – Brazil.
Experimental Research: Mechanical behavior characterization of the yarns (stress, strain, tenacity and linear weight); Dead weight test system to determine yarn creep curves. Low tension tests, between 15% and 30% of YBL (Yarn Break Load) to determine creep curves; High level tension (or short time) creep tests, using a mechanical test machine. High tensions between 50% and 90% of YBL. Creep comparative behavior of HMPE multifilament’s when submitted to changing conditions of temperature and load. Low temperature research.

- Adapted refrigerator - In this equipment the specimens can be suspended and tensioned by applying a load on the bottom causing a creep.

The tests were performed at low and ambient temperatures: 4°C, 20°C. Graph 3 shows the behavior of different kinds of HMPE at low temperature (4°C) and 30% of Yarn Break Load.

Graphic 3: Creep behavior, Low temperature

4. What do we start to do?

New possibilities and new horizons were opened through the pre-salt basin discovered in ultra deep water at the Santos basin in Brazil.

The location of the pre-salt Santos basin in ultra deep water we can see in figure 5.

Fig. 6: The pre-salt Santos basin location.

A typical array of offshore system in ultra deep water we can see in figure 6.

Fig. 7: The array of offshore system in ultra deep water.

4.1 New scenario and new challenges

According Rossi et alii [1] this is a new scenario at pre-salt petroleum basin in Brazil:
– Spread moored FPSO – WD 2000 to 2500m
– Small allowable offset and footprint radius
– Medium to harsh environment (Santos Basin)
– Need to avoid higher pretension level
– Simplify logistics whenever possible.

4.2 Offshore systems anchorage alternatives

Del Vecchio [2] shows the future possibilities regarding new materials and others synthetic ropes architectures:

All polyester ropes: for this alternative, Petrobras has many data obtained during the last 10 years (tenacity, MBL, creep and fatigue), but more is necessary to do regarding the durability (we don’t know so much about the effect of time and other natural effects and environment);

Stiffer fibers ropes: considering the great deep water we need to control and to reduce the rope strain and the spread of the platform.

We need to know the performance of those new fibers (HMPE, Kevlar, PEN, aramida...).

4.3 A new research project was started at POLICAB.

TECHNOLOGY DEVELOPMENT TO VALUATE THE SYNTHETIC MOORING ROPES, STRUCTURAL INTEGRITY, TO BE USED INTO AN OFFSHORE ANCHORAGE.

Through this project we intend to develop a theoretical and experimental methodology to guarantee the structural integrity of synthetic mooring ropes which are used in Stationary Production Units. The goal will be to obtain graphics and curves to evaluate the residual life and take decisions regarding ropes in work.

Beside the redundant system (the rope was made with parallel sub-ropes), the rope is a fragile
A link in the structural integrity chain due to its great responsibility to avoid global accidents.

The stress-life relation in usual work conditions is even unknown. This worry joined with the necessity to know better the behavior of those ropes along the work time.

Facing the new research possibilities we made a saloon with a tension and fatigue machine for synthetic mooring ropes, with hydraulic actuators MTS (150 tons in tension low cycle fatigue and 1,000 mm piston displacement) and HUNGER (300 tons in tension and 2,500 mm piston displacement). Machine operator and pressure generator system rooms (SilentFlo – MTS, works at 440 V, 3 pumps, 3,000 psi).

This gave us the possibility for new tests in real scale: rupture of synthetic mooring ropes and sub ropes, even 300 tons; Fatigue tests in tension to obtain curves of life for anchorages ropes with precise tension and displacement control; Stiffness after strain; Quasi-static stiffness; Dynamic stiffness; ISO 18692 [3] and Petrobras.

Figure 7 shows the final sketch of the machine: the 20 meters steel structure and the two actuators mounted.

![Figure 7](image)

**Fig. 7:** The saloon of test machine.

In figures 8 and 9 we show the new test machine working: 85 tons polyester sub rope, 12 strands in tension fatigue test.

![Figure 8](image)

**Fig. 8:** The test machine design.

![Figure 9](image)

**Fig. 9:** Test machine.

**5. Conclusion**

With this University - Petrobras joint project a research sector was created with large possibilities of development. POLICAB started as a research project and grow up naturally as a research and projects development laboratory, generating and transferring technology for the industries and, the most important, generating knowhow for our students. Today a significant number of students obtain scientific and technological knowledge related to ropes, anchorages, synthetic fibers and experimental analysis. The technological production, create and add knowledge. I believe: this is the most important GOAL at POLICAB!

**Acknowledgements:** POLICAB and related projects are supported by PETROBRAS.

**References**


EXPERIMENTAL AND THEORETICAL INVESTIGATIONS OF DORSIFLEXION ANGLE AND LIFE OF AN ANKLE – FOOT – ORTHOSIS MADE FROM (PERLON – CARBON FIBRE – ACRYLIC) AND POLYPROPYLENE MATERIALS

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Abstract

Experimental investigations in present paper were divided into: dorsiflexion and fatigue tests. Dorsiflexion test was done for (perlon-carbon fibre-acrylic) materials and ordinary solid designs of orthoses are 8.2° and 7.7° respectively using "Dartfish program". The fatigue test was achieved on prosthetic foot worn sole and measured life of sole found to be 2.137 x 10^6 cycles. The theoretical AFO lives were equal to 1.0 x 10^7 and 2.3 x 10^6 cycles for calf and sole parts respectively; while theoretical dorsiflexion angle of sole is equal to 8.266°.

Key words

AFO, Fatigue analysis, Perlon-Carbon fiber-Acrylic materials, Dartfish program, Sole life.

1. Introduction

Dartfish is the world's leading video analysis software for all sports, biomechanical and in broadcast applications, this technique moved it's focus to coaching applications. From skill analysis and game film breakdown to video casting. Dartfish helps coaches to teach more effectively, and athletes improve faster. Dartfish currently has over 100,000 clients, including Olympic federations, professional sports teams, academies, universities and clubs all over the world [1].

Researcher and instructors can use Dartfish to view and analyze video clips, capture skills and compare content. Dartfish allows to run comparisons, analyze movements and trajectories, calculate speeds and demonstrate key technique and position. Dartfish program was used to get the angular assessments from stick figures representations of the human leg, then the Spatial - temporal variables (angle, time, and distance) are measured during mid-stance (foot flat) and toe-off phases of gait cycle [2]. In free speed walking, human gait is a quasi-periodic activity with the left and right limbs out of phase. Control of the whole body, and specifically the lower limbs, is often described in terms of the magnitude of parameters at key events and during key phases that occur during the gait cycle [3].

The stresses measured in solid AFO were either tensile or compression stresses due to bending during motions. The maximum tensile stresses of the various and standard AFO were 0.35 MPa and 0.84 MPa respectively, while maximum compressive stresses 0.5 MPa and 0.6 MPa were located at the lower neck [4]. The strain gauge was bonded to the surface of the solid AFO, the strain information obtained in the solid polypropylene AFO during gait activities are the principal strain and allow determination of the maximum stress which maximum at middle – lower lateral neck [5]. Three orthoses were constructed with different curvatures cut out of the malleolar regions, these orthoses were then tested in failure for 500,000 cycles at 5 Hz in displacement control with initial displacements were set to provide maximum loads of 45 lbs [6]. High stress concentration in the neck region is consistent with the common clinical observation that AFO's break down in the neck region [7]. In present paper, experimental and theoretical investigations were done on new AFO made from two parts calf (made from PCA materials) and sole (made from PP material) connected together by rigid neck joint. Two more important items were measured and estimated here, dorsiflexion angle and life with comparison were made between experimental and theoretical results and between PCA calf and PP calf of AFO under fatigue loading.

2. Experimental results

2.1 Fixed plate test

The ground reaction force (GRF) was developed under sole, due to biomechanical effects on leg during gait and stance cases, can be tested for patient has drop foot in its left leg using fixed plate device or as called "ZEBRIS" connecting
directly to computer by UBS cables as shown in Fig.1. The GRF was determined in this test by let patient, 72 kgm weight has drop foot in left foot only, walking over fixed plate. The average temporal ground reaction force over gait cycle for left foot, drop foot worn AFO, can be shown in Fig. 2 which necessary to determine to applied next as input data in dynamic theoretical solution for sole.

![Fixed plate device](image)

**Fig. 1: Fixed plate device**

![Average force versus gait cycle for left foot](image)

**Fig. 2: Average force versus gait cycle for left foot**

### 2.2 Fatigue test

In current test, examination of a fatigue life of AFO experimentally was carried on by applying reciprocating and sequence of a forces on heel and toe – off regions of a standard prosthetic limb wear a sole using pneumatic fatigue device for prosthetic limbs tests. The magnitude of reciprocating force has been taken in our test is equal to 1.3 of patient weight (72 kgm) by suitable adjusting of frequency timer and setting compressor valve at 2.5 bar.

To avoid clearance between sole and prosthetic foot during operation, two 3 mm diameters screws were used, one of them at heel and second near toe – off zones. After test would been done, the sole failure has been detected as shown in Fig.3 due to introduce crack at the bottom surface of sole after (2137233) cycles.

![Sole fatigue failure after test](image)

**Fig. 3: Sole fatigue failure after test**

### 2.3 Material tensile and fatigue tests:

The fatigue solution for calf and sole parts of AFO orthosis are required stress – life curve which can be investigated using fatigue test device. To determine required parameters in calculations of amplitude stresses of PCA materials under cyclic reversed fatigue loading, by using monogram can be done by taking materials mechanical properties, Table 1, which investigated in current work by using tensile test.

<table>
<thead>
<tr>
<th>Material</th>
<th>Young's modulus E (GN/m²)</th>
<th>Yield stress σy (MN/m²)</th>
<th>Ultimate stress σu (MN/m²)</th>
<th>Possions ratio</th>
</tr>
</thead>
<tbody>
<tr>
<td>PCA</td>
<td>1.634</td>
<td>32</td>
<td>33.75</td>
<td>0.35</td>
</tr>
<tr>
<td>PP</td>
<td>0.812</td>
<td>21</td>
<td>28.75</td>
<td>0.42</td>
</tr>
</tbody>
</table>

Table 1: Mechanical properties for PCA and PP materials

The suitable range of deformation in bending test specimens which clearly determined from monogram are:

\[ \delta = 1.5 \text{ to } 4.75 \text{ mm}. \]

Fig.4 shows the investigated stress – life diagram for PCA materials when the bending deformation varies from 1.1 to 5.25 mm against number of cycles for which the specimen brick down. The stress – life curve for PP material was already available in references as shown in Fig.5. The best fit equations for two diagrams, Figs.4 and 5, under study can be investigated respectively as follows:

\[ \sigma = 0.921 (LOGN)^2 - 13.02 (LOGN) + 56.06 \quad (1) \]

\[ \sigma = -0.098 (LOGN)^3 + 1.824 (LOGN)^2 - 11.67 (LOGN) + 30.92 \quad (2) \]
2.4 Dorsiflexion test:
The temporal angle variation of dorsiflexion angle at beginning and end of gait for drop foot patient was being worn new and ordinary AFO using dartfish program can be shown in Figs. 6 and 7 respectively, while temporal angle for health man worn new AFO were clearly illustrated in Fig. 8.

![Fig. 6: Temporal dorsiflexion angle for patient wear present AFO, (a): t = 0, (b): t = 333 msec.]

![Fig. 7: Temporal dorsiflexion angle for patient wear ordinary solid AFO, (a): t = 0, (b): t = 333 msec.]

From previous images, the maximum dorsiflexion angles at toe-off phase are 8.2, 7.7, and 9.7 degrees for patient worn new design of AFO, patient himself worn ordinary solid AFO, and health man worn new orthoses respectively. The dorsiflexion angle variation with walking time steps are shown in Fig. 9 for three cases under study.

![Fig. 9: Dorsiflexion angle verses walking time.]

3. Theoretical results:
3.1 Dorsiflexion angle:
The theoretical vertical deflection of sole under static loading at toe-off region for patient has 72 kgm weight was 5.812 mm as shown in Fig. 10 while horizontal contact length between sole and ground during toe-off stance is 4 cm; therefore theoretical dorsiflexion angle equal to 8.266°.
3.2 Fatigue results:
The fatigue solution for PCA and PP calves has cut – out angle 25° under inner step-pressure loading over gait cycle shown in Fig.11 and by using investigated stress – life curves, Figs.4 and 5, can be done by ANSYS – WORKPENCH PROGRAM were illustrated its results in Figs.12, 13, 14, and 15 for total deformations, von – misses stresses, factor of safeties, and lives respectively.

Fig. 11: Pressure-gait cycle loading for fatigue analysis.

Fig. 12: Total deformation in (m) for 25° cut-out angle, a) PCA materials, b) PP material.

Fig. 13: Von – Misses stress in (N/m²) for 25° cut-out angle, a) PCA materials, b) PP material.

Fig. 14: Safety factor for 25° cut-out angle, a) PCA materials, b) PP material.

Fig. 15: Life in (cycles) for 25° cut-out angle, a) PCA materials, b) PP material.

The maximum deformations, Von – Misses stresses, and lives with minimum factors of safety induced in PCA and PP calves have cut – out angles 30° and 35° under fatigue loading can be determined and then collected with later results in Table 2.

Table 2: Fatigue results for calf has different cut – out angles

<table>
<thead>
<tr>
<th>Fatigue item</th>
<th>Cut – out angle (degree)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>25</td>
</tr>
<tr>
<td>Maximum deformation (mm)</td>
<td>PP</td>
</tr>
<tr>
<td></td>
<td>PCA</td>
</tr>
<tr>
<td>Maximum Von-Misses stress (N/mm²)</td>
<td>PP</td>
</tr>
<tr>
<td></td>
<td>PCA</td>
</tr>
<tr>
<td>Minimum factor of safety</td>
<td>PP</td>
</tr>
<tr>
<td></td>
<td>PCA</td>
</tr>
<tr>
<td>Life (cycle)</td>
<td>PP</td>
</tr>
<tr>
<td></td>
<td>PCA</td>
</tr>
</tbody>
</table>

The vertical deformation, Von – misses stress, factor of safety, and life induced due to fatigue loading on sole part can be shown in Fig.16. Table 3 was listed the experimental and theoretical investigation results in current paper for dorsiflexion angle and life of sole.
Discussion

The new design gave higher dorsiflexion angle, 8.2°, than ordinary one, 7.7°, by increasing in percentage amount of 6.1 %. The new design of AFO reduced the dorsiflexion angle from actual dorsiflexion of health man foot of a 10° to 9.7°, that’s means percentage reduction of 3 % because the PP sole work to prevent normally bending of foot to some less amount only. The theoretical dorsiflexion angle could be obtained only by tangent inverse of determined vertical deflection at sole tip divided by horizontal contact between foot (worn sole) to ground, which equal to 8.266°.

The maximum deformations were 0.16878 and 0.2514 mm for PCA and PP calves as shown in Fig.12, with reduction in deformation equal to 32.86 % from using PCA materials rather than PP material. Maximum von – Misses stresses were 30.11 and 15.03 N/mm² for PCA and PP materials as shown in Fig.13, and these values appears only in very small red regions near lower hole for strap fixation due to developing high stress concentration region around a hole. The actual maximum stress that have significant regions over calf’s surface area were 6.72 and 10.04 N/mm² for PCA and PP materials respectively, that means good reduction in stress was developed in PCA materials rather than PP one about 33.13 %.

Maximum factor of safety for new and ordinary materials was 15 lowered to 5 around fixed upper and lower holes for straps, where orange regions for PP material has greater area than against new materials as shown in Fig.14. Higher cycles to fatigue (life) was achieved 1.0 x 10⁷ cycles from using new material as shown in Fig.15 in comparison with only 2.3x 10⁶ cycles to calf failure for ordinary material with increment in life reached more than four times. The effect of selecting different cut – out angles on fatigue items results can be shown in Table 3 using two additional angles 30° and 35° for calf by using new materials only.

In sole fatigue calculations, the maximum deformation of sole can be detected as shown in Fig.16-a on tip of forend sole region which equal to 7.305 x 10⁻⁴ mm, while maximum dynamic von – misses was concentrated at the forend of sole wall (C – section) and equal to 4140 N/m² as shown in Fig.16-b. The maximum factor of safety was 15 while the overall life of new sole equal to 2.3 x 10⁶ cycles as shown in Fig.16-c and 16-d respectively.

A most important item for present study of AFO and in general biomechanical applications in prosthesis and orthosis were represented by dorsiflexion angle and sole life, therefore comparison between experimental and theoretical results had been done; Table 3 represents this comparison with only 0.8 % and 7.1 % differences for dorsiflexion angle and sole life respectively.

Conclusion

The major points can be concluded from present work are being listed in following points :

1. The PCA materials which used as calf materials instead of PP material was increase the calf life from 2.3 x 10⁶ to 1.0 x 10⁷ cycles, in addition to good reduction in fatigue deformation and Von - Misses stresses was obtained tend to 33.86 and 33.13 % respectively.

2. Increasing in dorsiflexion angle was obtained in present paper from 7.7° in ordinary solid AFO to 8.2° in new AFO made from PCA and PP materials.

3. Approximately identical results have been obtained for dorsiflexion angle of a new design of orthoses between experimental angle ( θexp. = 8.2°) using experimental images in dartfish program and theoretical angle ( θtheo. = 8.266° ) by using ANSYS program.
4. Experimental life of sole (2.137 x 10^6 cycles) while theoretical life (2.30 x 10^6 cycles) are investigated in present paper with good tendency of both results.

5. Dynamic factors of safety are higher than against static loading cases and its ranged between (5 – 15).

**Nomenclature**

- AFO: Ankle Foot Orthosis
- GRF: Ground reaction force
- PCA: Perlon – Carbon fibre – acrylic
- PP: Polypropylene

**References**

1. Introduction
With the planned construction of the fixed link across the straits known as the Fehmarn Belt, which will connect the island of Fehmarn in Germany with the Danish island of Lolland, a heavy increase in traffic is predicted on the Fehmarnsund Bridge, built in 1963. For this reason, it became necessary to verify available or new mathematical load bearing calculations against assessments acquired through measurement. This paper describes only the execution of measurements and their evaluation or interpretation; however, it does not determine any results nor does it derive any conclusions with regard to utilisation.

2. Combining calculation and metrological assessment to determine the load bearing capacity and life of railway bridges
2.1 General preliminary considerations
Deutsche Bahn alone owns about 28,000 railway bridges in various kinds of designs. Many of these bridges have already been in use for decades. Construction plans that are available are not always sufficient [1] to make reliable statements, for example, about the remaining life or changing conditions of utilisation, which is the case with the Fehmarnsund Bridge. Increasing traffic loads as well as demands for higher traversal speeds and the potential influences of environmental effects additionally urge the need for assessment of existing bridges. Adequate metrological methods combined with calculation models enable these tasks to be solved.

2.2 Measuring the system behaviour of bridges
This method involves an experimental investigation of the bridge under test considering both the loads during regular train traffic and partly substantially higher traffic loads. Increased loads are often required to obtain a measurable reaction of the structure. [2]. However, the loads are always within the range of the structure's elastic deformation behaviour and are predefined by the bridge surveyor/structural engineer. He also defines the number and type of measurement points in the different measurement sections as well as the type of dynamic loading, for example, due to defined crossings of vehicles at different speeds. Typical measurement quantities include strain, acceleration, displacement and temperature. This method involves a number of measuring points ranging between several tens and several hundreds. The duration of the measurement is often limited to hours (route closure, provisioning of loading vehicles, special wagons). The requirements test equipment needs to meet are defined by the dynamic and simultaneous recording of measured values.

3. Measuring the system behaviour on Fehmarnsund Bridge
3.1 Task and requirements
Initiated by discussions and first structural suggestions for building the fixed link across the straits (Fig. 1) connecting the island of Fehmarn (Germany) with Denmark, the Fehmarnsund Bridge, built in 1963, has been reconsidered as well. Utilization of this network arch bridge south of the planned link across the straits will substantially increase when a direct transport connection has been established both in terms of traffic density and prognosticated traffic load. The aim of system behaviour measurement from June 11 to 14, 2010 was to provide information on whether the structure would be able to cope with these requirements. This paper does not provide any assessment of the measurement results obtained. Assessment is based on Assessment Level 2 of Guideline 805 (Deutsche Bahn) and on the assessment guideline for road transport. Extensive measurements involving different loadings of both road and railroad track were required for calibrating the complex calculations models. The measurement program was
coordinated with the DB-Projektbau group of structural engineers.

Fig. 1: Geographical location – Fehmarnsund Bridge (Source: Wikipedia)

3.2 Measuring program

Discussions with structural engineers resulted in the conclusion that it would be expedient to define two measurement locations with associated measurement sections. Measurement location I including 3 measurement sections is located in the bridge's span and girder sections; measurement location II including 2 measurement sections is located on the network arch superstructure.

3.3 Type and installation of the transducers

Sensors were installed at 251 locations representative of the bridge's statics; they were to measure the deformation of the bridge structure resulting from variable cyclic loading. The predefined measuring points were fitted with sensors over the time period from April to June 2010. Most measuring points are based on the use of strain gauges (SG) in a full bridge circuit with one active bridge arm. This means that strain in response to applied load is measured in this strain gauge in the form of component expansion plus thermal expansion (Fig. 3). The other three SG bridge completions will not experience any component expansion resulting from applied load, because they are installed in areas that are not subjected to strain resulting from applied load.

Fig. 3: Strain gauges in a full bridge configuration (6-wire circuit)

Full bridge completion and the simultaneous recording of material expansion resulting from temperature variations in all four strain gauges enable these unwanted temperature effects to be almost fully compensated for. The strain gauge full bridge configuration permits electrical connection to downstream measurement electronics using a 6-wire circuit. Two additional sense leads electrically compensate for line effects resulting, for example, from temperature effects on long cables that would cause measurement errors.

Fig. 4: Strain gauges installation

3.4 Measurement data acquisition

Distributed data acquisition systems were installed for data acquisition and storage as well as for data transmission. Optical fibre technology was used for synchronization of data from both the distributed systems and the main control station, which is a requirement resulting from the dynamic acquisition of measured values. Figure 5 describes the basic topology of the measurement setup.
The distributed data acquisition systems were connected via fibre optic cable to enable data synchronization and control of the measuring system. The dimensions of the actual measurement setup as well as the complexity of the measurement task at hand become obvious by the fact that only through utilization of fibre optic cable could 60 km of usually required electrical connection cable be saved.

4. Execution of the measurements

Test loads were applied to the Fehmarnsund Bridge involving different trains of locomotives and additional heavy-duty trucks on the road from June 11 to 14, 2010 between 6:00 pm and 6:00 am next morning.

4.1 Quasi-static crossings of heavy-duty vehicles

Two heavy-duty vehicles crossed the bridge (12t/20t axle load) in 4 different lanes at speeds of v = 10 km/h and a distance of approximately 10 meters between the vehicles.

4.2 Crossings of a train of locomotives

This configuration involved a train of 2 x BR232 and 8 x BR155 locomotives. The total weight of a BR155 locomotive is 123 t with an average axle load of 22.5 t. The basic loading results in a vehicle weight of 6.276 t.

4.3 Combined crossings

This part of the measuring program involved combined crossings of the train of locomotives together with the heavy-duty trucks with different directions of travel and load patterns of the tracks and roads.

4.4 Dynamic measurements

Dynamic crossings were implemented at maximum speeds of approximately 120 km/h for the train of locomotives and about 80 km/h for the heavy-duty trucks.

Measurement was activated by light barriers installed in adequate locations.

5. Test results (examples)

Figures 9 and 10 provide examples for parallel crossings of the train of locomotives and the heavy-duty truck.
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Fig. 9: Parallel crossing of train of locomotives and heavy-duty truck at measurement location I (5-span continuous girder; centre of first span) – Stress distribution at the floor panels

Fig. 10: Parallel crossing of train of locomotives and heavy-duty truck at measurement location I (5-span continuous girder; centre of first span) – Stress conditions at cross girder, measurement section 2 (guide barrier)

Note
The current report is based on written or verbal information, explanations and images from the Department of Bridge Measurement of the Deutsche Bahn (German National Railways) in Magdeburg. Many thanks are due to Messrs. Volkmar Quoos, Peter Krempels and Uwe Friebe for their helpful assistance and support.

References
1. Introduction

Knowledge of real material parameters and its behaviour is nowadays a condition for numerical simulations, widely used in industry to design quality components in very short time. The paper presents the method of Electronic Speckle Pattern Interferometry (ESPI) to get the coefficient of thermal expansion of carbon fibres laminates and behaviour of the high strength steel sheets during the unloading process after large plastic deformations.

ESPI is an optical measuring technique that allows highly accurate measurement of deformations. In comparison with other techniques for strain measurement the ESPI enjoys the advantages of being non-contact, full-field, has a high spatial resolution, high sensitivity, delivers accurate displacement data and does not require any calibration or costly surface preparation. It can be applied to any material provided that the surface is sufficiently rough and the laser light is diffusely reflected.

In the following the in-plane speckle interferometer principle [1,2], also called double illumination principle, will be explained (Fig.1). The laser beam is split in two beams at an angle of $2\theta$ using a beam splitter (e.g. diffraction grating). These two object beams generate their own speckle patterns which are added coherently and form a resulting subjective speckle at the detector of a CCD camera. Correlation fringes which occur by subtracting the speckle pattern of the object in two stages (unloaded & loaded) represent contours of equal in-plane displacement component parallel to the plane containing the two illumination beams. The displacement can be calculated when the phase change $\Delta \phi(x,y)$ is known according to the formula

$$\Delta \phi(x,y) = \frac{\Delta u_x(x,y)}{\sin \theta}.$$  

The phase determination relies on temporal phase shifting technique (i.e. a four phase algorithm [1,2]).

As beam splitter a diffraction grating (1600 lines/mm) was used. The “+1” and “-1” diffraction orders are used as illumination beams and the “0” order is absorbed. A piezo-translator mounted on a mirror produce the phase shifting effect. For a Nd:YAG laser with a wavelength of 523 nm the in-plane measuring sensitivity of the set-up is 0,417nm.

2. Experimental Results

First presented application of the ESPI measuring system is determination of the coefficient of thermal expansion (CTE) for some carbon fibres laminates [3]. Carbon fibre laminated sheets have multiple industrial applications, products with low thickness being particularly required for lightweight structures.

Measurement of the CTE for anisotropic materials such as carbon fibre composites using ESPI offers not only a final value but also full-field information about the deformation of the material under thermal stress, especially if the mismatch between CTE of matrix and fibres are taken into account. Examples of phase maps in
dependence with fibre directions for the unidirectional laminate are presented in Fig. 2.

Fig. 2: Phase maps of unidirectional laminate for fibres direction: (a) perpendicular to measuring direction, (b) parallel with measuring direction, (c) rotated -45° from measuring direction.

It could be proved that the optical method of ESPI has special advantages for the investigation of thin (< 1 mm) and small (< 10 mm) specimens, for which other methods cannot be applied. Another advantage is that the fringe or phase maps give important information about the uniformity or non-uniformity of the strain field in the specimen as for example in composites. The overall accuracy of the CTE measurement by the suggested method was estimated at ≈ 0.1x10^{-6} [1/K].

Another application of ESPI technique refers to investigation of non-linear springback for high strength steel sheets [4]. The springback prediction in deep drawing is an important issue for the production of car bodies in the automotive industry. The springback of the sheet metals after large deformations during deep drawing is not a strongly linear process with a constant Young’s modulus but, the stress-strain behaviour during the unloading phases, shows considerably non-linear and inelastic effects. Unloading of two types of steel sheets for cold forming, a cold-rolled high strength micro-alloyed steel and a low carbon steel sheet, have been analysed by ESPI. The specimens were investigated by uniaxial tension tests, and the influences of different testing parameters upon springback were analyzed.

The experimental measurements showed that the stress-strain curve during unloading is non-linear, the influence of the prestrain path upon unloading is minor and the secant moduli of unloading curves decrease with increasing of prestrain. When the prestrain value becomes high enough, a saturated value for the secant modulus is approached.

Acknowledgements: This research was supported by the Department of Experimental Mechanics from Chemnitz University of Technology, Germany and the Alexander von Humboldt Stiftung/Foundation.

References

IN-PLANE MOIRÉ TECHNIQUES IN THE EXPERIMENTAL SOLID MECHANICS – A SHORT SURVEY

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1. Introduction
In the experimental mechanics the Moiré technique has developed to an established method. At the Chemnitz University moiré methods were investigated since 1965 [1].

To generate a moiré effect two gratings are superimposed. The moiré fringes can be interpreted as parameter curves, Fig. 1.

Fig. 1: Moiré fringes as parameter lines
Along each moiré fringe (order \( m \)) the difference \( m = l - k \) of the line orders is constant.

2. Geometric Moiré
In the geometric moire technique an object grating deformed with the specimen and a reference grating are superimposed.

Fig. 2: Undeformed object grating

- The undeformed object grating, Fig. 2, is described by:
  \[ x_2^1 = -\frac{x_1^1}{\tan \xi} + \frac{k \cdot p}{\sin \xi} \]
  \( x_i^1 \) … co-ordinates before deformation

- As deformation process is assumed a homogeneous deformation state with the displacements \( u_i \) and \( u_{ij} = \text{const} \):
  \[
  u_1(x_1, x_2) = u_{1,1} \cdot x_1 + u_{1,2} \cdot x_2 \\
  u_2(x_1, x_2) = u_{2,1} \cdot x_1 + u_{2,2} \cdot x_2 \\
  \]
  \( x_i \) … co-ordinates after deformation

- The resulting deformed object grating is
  \[
  x_2 = \frac{-(1 - u_{1,1})\cos \xi + u_{2,1} \cdot \sin \xi}{(1 - u_{2,2})\sin \xi - u_{1,2} \cdot \cos \xi} x_1 + k \cdot p \\
  \]

- The equation for the reference grating is
  \[
  x_2 = -\frac{x_1}{\tan(\xi + \Delta \xi)} + \frac{l \cdot p \cdot (1 + \delta)}{\sin(\xi + \Delta \xi)} \\
  \delta, \Delta \xi \ldots \text{mismatch of the reference grating}
  \]

- Superposition of the deformed object grating and the reference grating with \( m = l - k \) and for the simple case with \( \delta = \Delta \xi = 0 \) gives:
  \[
  p \cdot m = x_1(u_{1,1} \cdot \cos \xi + u_{2,1} \cdot \sin \xi) \\
  + x_2(u_{1,2} \cdot \cos \xi + u_{2,2} \cdot \sin \xi) \\
  \]
  respectively \( u_2(x_1, x_2) = p \cdot m_2(x_1, x_2) \).

These moiré fringes are named as isothetics.

Fig. 3 shows an example for the ordering of isothetic fields [3]. In practice the ordering is realized intuitively and not by \( m = l - k \).

Fig. 3: Bending of a highelastic beam (13.3L/mm)
An important application of the geometric moiré is the visualization and measurement of large plastic deformations, Fig. 4, [3].

Fig. 4: Compound Extrusion Process of a bimetallic rod at room temperature (25 L/mm)

3. Interferometric Moiré

Optical foundations of the interferometric moiré are the diffraction at gratings and the optical interference [2],[3]. The generated fringes are called "moiré fringes" also. Two fundamental setups can be distinguished:
- Moiré fringe multiplication, Fig. 5a
- Moiré interferometry, Fig. 5b

The sensitivity of moiré fringe multiplication is [2],[3]:

\[ u_\xi(x_1, x_2) = p_{eff} \cdot m_\xi(x_1, x_2) \quad \text{with} \quad p_{eff} = \frac{p}{r} \]

Fig. 6 shows the optical multiplication of an isothetic field. The object grating has only a real density of 50 L/mm.

Fig. 6: Moiré fringe multiplication at a diametrically compressed disk [3]. 50..500 L/mm (p_{eff}=20..2 μm)

The Moiré interferometry is world-wide used thanks to the activities of Post [6].

The experimental setup of moiré-interferometry, Fig. 5b, is characterized by two plane coherent waves diffracting at the deformed object grating in nearly the same direction. The resulting sensitivity is

\[ u_\xi(x_1, x_2) = p_{eff} \cdot m_\xi(x_1, x_2) \quad \text{with} \quad p_{eff} = \frac{p}{2n} \]

One of the most important application fields is the analysis of microstructures with high density object gratings, Fig. 7.

Fig. 7: Micro moiré interferometry to investigate the strain transfer at a strain gage [4],[5]. 1400 L/mm, n=1, p_{eff}=357 nm

References


ACOUSTIC EMISSION – THE SENTRY FUNCTION

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1. Introduction
The acoustic emission (AE) technique has been used for decades to detect damage onset and propagation in different kind of materials [1]. The more complex microstructure of the material, the more information can be derived from the AE signal [2]. Solid mechanics experimentalist are familiar with the acoustic emission produced by the material during the loading phase, which sometimes can be heard simply by naked ears. In fact, during a material test or in general when a component is subject to external loads, a rapid stress redistribution can occur due to permanent and irreversible phenomena, caused by damage mechanisms. During this redistribution, part of the strain energy stored in the material is released in the form of heat and of elastic waves that propagate in the material until they reach the free surface. These transient elastic waves are commonly detected as acoustic waves. Some acoustic emission can be also produced by mechanisms different from damage (such as sliding and friction of two surfaces in contact) and this must be taken into account. The elastic waves propagating at the component surfaces are detected by means of piezoelectric devices that convert the mechanical signal into an electrical one.

Even if the AE physical principle is very simple and immediate, the use of this technique is not so straightforward because the acoustic wave propagation in solids is quite complicated. Multiple waves that propagate with different velocities, reflection, refraction, dispersion, and attenuation, may affect the measured signal. Nevertheless some advantages with respect to other nondestructive testing techniques can be found in the possibility to monitor a large volume of material by means of few sensors able to locate the damage by triangulation and to make it continuous during real life service. In reality, the acoustic emission is produced within the material itself once loaded at a level that produces some form of damage. In this sense, it is not strictly a non destructive testing method since it is based on passive monitoring of acoustic energy released by the material or structure itself while under load. Mechanical information and AE information can be analyzed separately to determine damage in the structure. However, when one is taken into account and the other is omitted a comprehensive damage characterization cannot be taken out. In this paper some possible application of a recently defined function [3,4] called Sentry (SF), to the damage identification and residual strength determination in the case of composite laminates, is shown.

2. Definition of the Sentry Function
In order to perform a deeper analysis of the laminate behavior, a function that combines both the mechanical and acoustic energy information is employed. This function is expressed in terms of the logarithm of the ratio between the strain energy (Es) and the acoustic energy (Ea), where x is the test driving variable (usually displacement or strain).

$$f(x) = \ln \left[ \frac{E_a(x)}{E_s(x)} \right]$$

The function f(x) is divided into five distinct areas: an increasing function PI(x), a sudden drop function PII(x), a constant function PIII(x) a decreasing function PIV(x) and a sometimes a Bottom-up function BU(x). Each region represents a specific stage in the damage process (Fig. 1). The sentry function, type PI, represents the strain energy storing phase when it is increasing. During the test the ability of the material to store energy reaches its limits and the AE cumulative energy significantly increases due to damage progression hence the slope of the PI(x) function decreases. During the damage process when a major failure takes place in the material the stored mechanical energy is suddenly released producing a ratio of this energy as acoustic waves. This is shown by the abrupt drop in the function f(x) that is described by the type II function, PII(x).
After each major failure, type II function, the slope of the next PI(x) function decreases, until the material largely loses its ability to store mechanical energy. At this stage the slope of the function reaches zero or below zero, type III or IV function.

The Bottom-Up (BU) trend indicates that a strengthening event induced an instantaneous energy storing capability in the material. Such an event can be related to hardening effects, self-healing effects or, as in the case of the present study, it can be related to fiber bridging effects.

3. Damage identification

The function defined in the previous paragraph was named “Sentry” for its capability of highlighting important damage events.

In Fig. 2 it is shown the first application [3] to composite laminates. Different lay-ups show different sentry functions. Fig. 3 and Fig. 4 show a more recent application [5] to composite tubes loaded in torsion after an accidental impact.

In this case the shape and composition of the SF depend from the lay-up and also from the impact event characteristics.

A completely different application has been done by the group of the Amirkabir University [6, 7]. They used the Sentry function for the characterization of inter-laminar properties of CFRP by means of DCB specimens shown in Fig. 5. In particular in [6] the SF was utilized to detect initiation of delamination and to distinguish different kinds of damages in different regions in mode I delamination test. This approach simply discerns the behaviour of the material in different
stages of initiation. The damage mechanisms have been verified using SEM images.

Fig. 6: DCB specimens for the characterization of the inter-laminar properties in CFRP

On the other hand in [7] the Integral of the SF was used, as explained in the following paragraph.

4. Residual strength determination

From the consideration that the SF is linked to damage processes it is was supposed that the integral of SF, called Int(f), over the acoustic emission domain (in terms of the test driving variable) was related in same way to the residual strength or to the resistance to crack propagation.

\[ \text{Int}(f) = \int_{\Omega_{AE}} f(x) \, dx \]

In [4] Minak & Zucchelli derived a phenomenological relation between the static residual strenght in traction for different lay-ups and the Int(f) for transversally indented laminates, that is shown in Fig. 7.

Fig. 7: Relation between residual tensile strength and INT(f) for two different lay-ups

By means of that relation they and Morelli [8] found that it is possible to pool the results regarding the tensile fatigue life for damaged and undamaged specimens (Fig. 8)

This was done referring the load levels to the residual tensile strenght derived from Fig.7 rather than to the tensile strength of the undamaged specimens.

Fig. 8: Pooling of tensile fatigue life of damaged and undamaged specimens by means of Int(f)

As said before, Oskuei et al. [7] showed that, for DCB specimens (Fig. 6) based on values of SF it was possible to predict the test stage at which a delamination propagation becomes visible under an opening load condition. Furthermore, it was possible to highlight a bi-linear relation (see Fig. 9) between the cumulative strain energy release rate (GICUM) and Int(f). The transition point in the bi-linear relation enabled the estimation of the critical strain release rate (Gl) value. The Gl obtained by this approach was then compared to the values obtained by both ASTM D5528 standard test method and Ndiaye approaches.

Fig. 9: Bilinear relation between GICUM and Int(f)

Gl calculated from the SF method is in good agreement with the results obtained from the
ASTM D5528 method (5% max Load) (Fig.10). Moreover it can be also noted that the standard deviation related to the results obtained by the new method are smaller than the ones obtained applying the ASTM D5528 methods. It can thus be concluded that the sentry function is another alternative method for calculating GIc directly from acoustic emission data.

Fig. 10: Estimation of GIc

5. Conclusion
In this paper the capabilities of the Sentry function and of its integral in different case studies regarding the mechanical behaviour of composite laminates are shown. Young researchers who want to use the method exposed to solve their problems, or who already did it, may feel free to contact the author or DR Andrea Zucchelli (a.zucchelli@unibo.it) for support or discussion.

Acknowledgements: The author thanks DR. Andrea Zucchelli who is the first inventor of the method shown in this paper.

References
SYMPOSIUM SESSION I
EXPERIMENTAL AND NUMERICAL CHARACTERIZATION OF SINTERIZED MATERIALS WITH SPECKLE INTERFEROMETRY AND OPTIMIZATION METHODS

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1. Introduction

The traditional approach to mechanically characterize materials is to perform destructive tests. When material properties are known, the displacement field of a specimen generally loaded and constrained is univocally defined. This paper suggests an innovative methodology in order to determine mechanical characteristics (E, ν) of new materials for an inverse solution of the elastic problem. A hybrid approach based on the combination of phase-shifting electronic speckle pattern interferometry (PS-ESPI) and finite element analysis is utilized. An innovative algorithm, implemented in the numerical model, automatically executes several optimization loops varying E and ν in order to minimize the objective function based on the difference between displacements evaluated by means of ESPI and the same predicted by FEM analysis. Three-point-bending experimental tests are carried out on titanium alloy and Selective Laser Melting (SLM) specimens.

2. Experimental Tests

Preliminary tests on isotropic and homogeneous titanium alloy were planned in order to define the experimental set up and to validate the numerical model that could be used to study new materials, as SLM. SLM parts are composed overlapping layers of metal powder. A focused laser beam selectively fuses powdered material by scanning cross-sections generated from a 3D CAD model of the part on the surface of a powder bed. In order to obtain high density parts the powder mixture and SLM process parameters are optimized.

Electronic Speckle Pattern Interferometry (ESPI) [1,2] is a non-contact optical technique, which accurately measures displacements in real time, gathering full field information without altering specimen conditions. ESPI can measure displacement components u(x,y,z), v(x,y,z) and w(x,y,z) for each point (x,y,z) of the specimen surface. Fringes will appear on the specimen surface and each fringe represents the locus of an iso-displacement region. The frequency distribution of fringes can be used to evaluate strain fields.

Three-point bending tests were carried out on prismatic samples at various loading step. Figure 1 shows the loading apparatus. It connects a 2 kg loading cell to the wedge, in order to measure the applied load. The load was transferred by a micrometric translational stage, which pushes the loading wedge against the specimen. Preloading beams ensures to minimize rigid body motions, which may cause speckle decorrelation. Data were recorded by the Micro Measurements System 5000 acquisition system. The optical set-up used for measuring u-displacements consists of a double illumination Lendertz interferometer [1,2].

![Fig. 1: Loading apparatus](image)

3. Numerical Model

A finite element model, with real sizes of the specimens tested, was realized. Kinematic constraints were imposed in order to correctly reproduce the mechanism of loading. FE analysis was carried out with the ANSYS® 10.0 commercial software [3]. The specimen was modeled with SOLID45 three-dimensional elements. Although specimens’ thickness is small compared to length, the specimen under 3-point-bending was however modeled as a 3D specimen.
in order to account for asymmetries eventually occurring in the loading process or related with constraint conditions.

Fig. 2: Finite element model of the 3-point-bending test numerically simulated

Specimen materials were assumed as isotropic and linearly elastic. This assumption is obviously more realistic for the titanium specimen than for the sintered one, because SLM specimens are realized by random strategy on each layer, so each layer could be considered as isotropic. The experimental evidence seems to confirm this assumption: in fact, the $u_x$ displacement measured through the thickness became null at the specimen midplane. The mesh included 293112 nodes and 265232 elements. (Figure 2 with indications of loads and constraints is representative of both materials). Mesh size was consistent with sampling of the speckle pattern. All finite element analyses were run on a standard PC equipped by an Intel® Core™ i7 processor and 8GB RAM memory. The structural analysis supplies $E$ and $\nu$ values and was completed in about 10 hours.

### 4. Experimental Results

The horizontal displacement measured by ESPI was taken as target value of FE analysis. The algorithm implemented compares these values with the same calculated numerically for each optimization loop, until the gap was minimized. The area monitored during experimental test was located 500 nm far from the constraint wedge and far enough from the region where the load is applied, in order to avoid the influence of local phenomena. Starting values of Young’s modulus and Poisson ratio for both materials in numerical models were respectively 180 GPa and 0.3. After FEM validation of titanium specimens, SLM component was analyzed by the same code changing only geometrical dimensions. Young’s modulus and Poisson coefficient obtained for titanium alloy by means of the proposed hybrid procedure were respectively 109 GPa and 0.299.

Figure 3 summarizes the convergence procedure on titanium specimen for one loading step, in order to validate the algorithm function used in the optimization procedure.

Fig. 3: Algorithm function of the optimization cycle

Table 1 shows experimental and FEM results for SLM specimen. The proposed methodology seems to work well. The residual error on displacements is minor than 1%.

<table>
<thead>
<tr>
<th>Load [g]</th>
<th>$\Delta u_{Exp fit}$ [nm]</th>
<th>$\Delta u_{FEM}$ [nm]</th>
<th>Error [%]*E-05</th>
<th>$E$ [GPa]</th>
<th>$\nu$</th>
</tr>
</thead>
<tbody>
<tr>
<td>293</td>
<td>489.78</td>
<td>489.784</td>
<td>2.5</td>
<td>131.33</td>
<td>0.299</td>
</tr>
<tr>
<td>330</td>
<td>550.73</td>
<td>550.729</td>
<td>0.29</td>
<td>131.54</td>
<td>0.299</td>
</tr>
<tr>
<td>504</td>
<td>837.34</td>
<td>837.340</td>
<td>0.026</td>
<td>132.13</td>
<td>0.299</td>
</tr>
<tr>
<td>529</td>
<td>878.52</td>
<td>878.520</td>
<td>0.015</td>
<td>132.19</td>
<td>0.299</td>
</tr>
<tr>
<td>695</td>
<td>1151.95</td>
<td>1151.950</td>
<td>0.052</td>
<td>132.45</td>
<td>0.299</td>
</tr>
<tr>
<td>706</td>
<td>1170.07</td>
<td>1170.069</td>
<td>0.22</td>
<td>132.46</td>
<td>0.299</td>
</tr>
<tr>
<td>810</td>
<td>1341.38</td>
<td>1341.382</td>
<td>0.12</td>
<td>132.56</td>
<td>0.299</td>
</tr>
</tbody>
</table>

Table 1: Percentage displacements’ difference on SLM specimen

Mechanical properties calculated for SLM specimen were: Young’s modulus 132 GPa and 0.299 Poisson coefficient. The experimental pattern is in excellent agreement with FE results. It results important that the weight of Poisson’s ratio in FEM optimization is absolutely inferior than the Young’s modulus.

### References


1. Introduction

Cutting forces in turning process are analyzed in this paper. Workpiece has a circular cross section with a slot, Figure 1. The measurements are performed using dynamometer made from turning tool with tensometric tapes placed on it [1]. The main cutting force $F_c$, radial force $F_r$ and feed force $F_f$ are obtained by recalculating of the measured strains on turning tool.

The aim of this paper is to determine the effect of slot on the cutting forces in turning process in order to determine optimal processing parameters. Turning of workpiece with a slot rarely occurs in practice, but that case is analogous to the milling in case when the milling tool enters the workpiece.

2. Cutting forces in the dependence of cutting speed

Constant parameters for this measurement are: cutting depth: $a = 3$ mm and feed rate: $f = 0.18$ mm/rev. The measurement was performed three times with following cutting speeds: $v_1 = 69$ m/min, $v_2 = 97$ m/min, $v_3 = 138$ m/min. Data acquisition was performed with a frequency of 2.4 kHz.

The diagrams on Figure 2 show the measured forces for the above mentioned parameters.

![Fig. 2: Cutting forces in the dependence of cutting speed](image)

By analyzing above diagrams it can be concluded that by increasing of cutting speed there is no significantly affect on the increasing of the cutting forces. It is obvious from the diagrams that there is an impact that occurs during the passage of turning tool blade through a rectangular slot and repeated entry into the material. The amplitude of impact is significantly higher in case of turning with lower cutting speeds. At higher cutting speeds, cutting tool has less time to return to the unloaded state when passing over the slot than in case of turning with lower cutting speeds. Therefore, with increasing of cutting speed, uniform forces with smaller impact amplitudes are obtained.

3. Cutting forces in dependence of cutting depth

The diagrams on Figure 3 show the measured forces for the turning of workpiece with a
rectangular slot and for various cutting depth. In this case, cutting speed \((v = 97 \text{ m/min})\) and feed rate \((f = 0.18 \text{ mm/rev})\) were constant, and measurements were performed for three value of cutting depth: \(a = 1 \text{ mm}, a = 2 \text{ mm}, a = 3 \text{ mm}\).

![Fig. 3: Cutting forces in dependence of cutting depth](image)

As expected and obvious from Figure 3, by increasing of cutting depth, cutting forces are also increasing as well as the amplitude of impact. As mentioned earlier, impact occurs during the passage of turning tool blade through a rectangular slot and repeated entry into the material. These impacts cause shortening of lifetime of turning tool blade. When operating with small cutting depth \((a = 1 \text{ mm})\), vibrations of turning tool occurs when it passes through the slot, resulting with poorer quality of surface.

4. Cutting forces in dependence of feed rate

The diagrams on Figure 4 show the measured forces for the turning of workpiece with a rectangular slot with cutting speed of 97 m/min and cutting depth of 2 mm. In this case, measurements were performed for three value of feed rate: \(f_1 = 0.18 \text{ mm/rev}, f_2 = 0.24 \text{ mm/rev} \text{ and } f_3 = 0.3 \text{ mm/rev}\).

![Fig. 4: Cutting forces in dependence of feed rate](image)

It is obvious from the Figure 4 that by increasing of feed rate, cutting forces are also increasing, especially the main cutting force \(F_c\). Increasing of feed rate does not significantly affect to the amplitude of impact which occurs during the passage of turning tool blade through a rectangular slot and repeated entry into the material.

References

CORROSION RESISTANCE OF THE 11SMN37 STEEL IN THE POLYMER CONCRETE COVERING

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1. Introduction

In the alkaline environment of the concrete covering a thin layer of invisible ferric oxide (Fe₂O₃) is created at the steel surface, which protects the steel against corrosion [1]. A drop in the concrete pore fluid pH to the value of 11.8 results in destruction of the passive layer, which in turn increases the rate of corrosion [2].

Ammonium chloride (NH₄Cl) contained in industrial waste of the artificial nitrogenous fertiliser plant is reacting with all phases of the concrete covering [3]. Changes in the cement grout phase composition and migration of chloride ions inwards concrete deteriorate protective properties of the concrete covering and increase probability of the reinforcing steel corrosion [4]. Protection of the concrete material surface and structure is frequently related to introducing polymer additives to cement. Thanks to that, a significant improvement in the concrete mixture quality and concrete usefulness have been achieved.

2. Research methodology

Bars of the 11Smn37 grade steel of about 8 mm in diameter placed in the polymer concrete covering made of the acrylic resin based polymer-cement mixtures were used for the tests.

The test samples were marked as follows:
- Sample No 1: steel bar included in the polymer concrete covering from producer A.
- Sample No 2: steel bar included in the polymer concrete covering from producer B.

Chemical analysis of the steel bars used in the research has been presented in Table 1

<table>
<thead>
<tr>
<th>%C</th>
<th>%Mn</th>
<th>%Si</th>
<th>%P</th>
<th>%S</th>
</tr>
</thead>
<tbody>
<tr>
<td>≤ 0.14</td>
<td>1.00-1.50</td>
<td>≤ 0.05</td>
<td>≤ 0.110</td>
<td>0.340-0.400</td>
</tr>
</tbody>
</table>

3. Impedance spectroscopy method

Corrosion tests using the impedance spectroscopy method were performed in the ammonium chloride solution NH₄Cl with pH indicator of about 5.

Impedance measurements were performed using the IM6e equipment from Zahner, and the measuring and calculation programs.

The tests were conducted within 1 mHz to 100 kHz frequency range and the amplitude value was 10 mV at stationary potential.

Wide range of the test frequency enabled interpretation of the electrochemical processes appearing at the steel surface in contact with the concrete.

4. Impedance spectroscopy test results

Measurement results from impedance spectroscopy, in the form of a series of impedance spectra for steel bars in the test corrosive environment, have been presented in Fig. 2 (a,b).

In the low frequency range, impedance spectra are visible in the shape of ever smaller semicircles. Diminishing of the impedance spectra semicircles for the steel in polymer concrete covering from producers A (Fig. 2, a) and B (Fig. 2, b) begins yet from the 6th week of exposure. That could suggest a loss of protective properties by the protective layer created at the steel surface. Beginning with the 6th week of exposure, resistance of the protective layer in sample No 1 decreased to 139 Ohm (Fig. 2, a), and in sample No 2 do 480 Ohm (Fig. 2, b). It is known from the subject literature...
that the bigger resistance of the protective layer the better it protects the steel surface against corrosion.

Fig. 2. Nyquist impedance diagram:

a) Sample No 1
b) Sample No 2 (1 – 1st week, 2 – 3rd week, 3 – 6th week, 4 – 10th week, 5 -15th week, 6 – 21st week, 7 – 28th week of exposure).

5. Tensile strength test results

Strength tests of the 11SMn37 steel grade were performed at the testing machine with the valid Calibration Certificate, according to the required procedure. Samples were subjected to axial force and loaded until rupture.

On performing the tensile strength tests, a brake in sample No 1 and sample No 2 happened in places weakened the most by the corrosion processes.

The tensile strength test results for samples No 1 and 2, after the 28-week exposure period to the corrosive environment, have been collected in Table 2.

Table 2. Tensile strength test results

<table>
<thead>
<tr>
<th>Sample Number</th>
<th>Fm, [N]</th>
<th>Rp0.2, [MPa]</th>
<th>Rm, [MPa]</th>
<th>A, %</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>27000</td>
<td>506</td>
<td>558</td>
<td>10,0</td>
</tr>
<tr>
<td>2</td>
<td>28500</td>
<td>---</td>
<td>574</td>
<td>10,0</td>
</tr>
</tbody>
</table>

Fig. 6 (a, b) presents fractures of samples No 1 and 2 observed at magnification of 4000x.

As a result of the fractographic tests the brittle-plastic fracture of the ferritic – pearlitic structure with non-metallic inclusions in the form of manganese sulfides was found.

6. Conclusion

1) The research performed with impedance spectroscopy method have shown that steel grade 11SMn37 in the polymer concrete covering from producer A (sample No 1) is less resistant to corrosion than the same steel in the polymer concrete covering from producer B (sample No 2), (Fig. 2 (a,b)).

2) The tensile strength tests have shown, that values obtained for both samples stay within the allowed range according to the PN/H EN 10087 Standard, i.e. from 510 MPa to 810 MPa. Comparing the Rm values for sample No 1 (Rm = 558 MPa) and sample No 2 (Rm = 574 MPa), a conclusion could be drawn that a bar made of the 11SMn37 grade steel in the polymer concrete covering from producer B is better protected against corrosion than the bar in covering from producer A (Table 2).

3) The fractographic test results for samples No 1 and 2 have shown the brittle-plastic fracture.

References

INFLUENCE OF MICRO-STRUCTURE ON FIBRE PUSH-OUT TESTS

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1. Introduction
The investigations presented in this paper focus on the simulation of push-out tests on a long fibre reinforced thermoplastic (LFT) manufactured in a compression moulding process. The microstructure of LFT is strongly influenced by the manufacturing process and shows inhomogeneous distribution and orientation of the glass fibres. Figure 1 shows the surface of a polished specimen, which has been cut normal to the melt flow direction.

![Surface of polished LFT specimen](source: IWK 1)

Fig. 1: Surface of polished LFT specimen

The influence of geometrical parameters like, e.g., fibre arrangements and fibre misalignment is still not fully understood. For the interpretation of experimental data the afore-mentioned parameters must be taken into account. Therefore, a numerical study is performed to determine these influences on the mechanical behaviour in push-out experiments.

2. Numerical Methods
For the finite element simulation, a linear-elastic behaviour of glass and polypropylene has been assumed with the additional assumption that the two phases (i.e. the matrix and the fibres) are initially perfectly bonded. To describe the interface model, the separation of the surfaces at the interface $\delta$ will be related by the isotropic effective interface stiffness $K$ due to ongoing damaging process with the traction on the interface $t$ by $t = K \delta$ \cite{1}. This effective stiffness $K$ is related to the initial stiffness $K^0$ by the scalar damage variable $D$ via $K = (1-D)K^0$. Starting from an undamaged interface $K = K^0$, i.e. $D = 0$, the onset of interface damage is described by the pressure-independent damage initiation criterion $\varphi(t^*) = \frac{1}{G} \sigma^* \cdot (G t^*) \leq 1$, $G = (\tau_c)^2 I$, $t^* = (I-n \otimes n) t + \langle t\cdot n \rangle n$.

Hence, the isotropic second-order damage initiation tensor $G$ depends only on one interface strength $\tau_c$. To describe the evolution of the damage variable $D$, the effective separation $\delta_e = \| \delta \|$ and its maximal value $\delta_{e, \text{max}}$ over the time history have been used together with the effective separation at the damage initiation $\delta_e^0$ and the effective separation at the complete interface failure $\delta_e^f$:

$$D = \frac{\delta_e^f}{\delta_e^f - \delta_e^0} \left(1 - \frac{\delta_e^0}{\delta_{e, \text{max}}} \right).$$

According to the investigations of Zhandarov et al. \cite{2}, the interface strength has been assumed as $\tau_c = 18$ MPa, and the interface fracture energy to $\Gamma = 2.96$ J/m². These parameters have been used for all simulations. The fibre diameter is 10 µm, the specimen thickness is $h=100$ µm. For the simulation of push-out tests, the interface model provided by ABAQUS has been used.

3. Results
Figure 2 shows the shear traction, the damage initiation criterion $\varphi(t^*)$ and the damage variable $D$ on the interface at the maximum force of the indenter, for the reference model with a single and centric embedded fibre which is oriented parallel to the push-out direction.
Fig. 2: Plot of the interface field variables for the reference model.

It is apparent that after the onset of interface damage the reaction force on the indenter is still increasing. For this reason, the value of the peak force depends on the damage model, the assumed interface strength $\tau_c$, and the fracture energy $\Gamma$. To study the sensitivity of the indentation force peak with respect to varying microstructures, the results from the simulations with modified position or orientation of the single fibre as well as the simulation with interacting fibres are compared to the reference model. All the variations of the topological parameters have been calculated stepwise in defined ranges. The influence of the eccentricity has been calculated in a range of 40 $\mu$m in steps of 5 $\mu$m, the influence of the inclination angle has been calculated from 0° to 25° in 5° steps, and for the fibre interaction models, the distance between the fibres has been varied in 2 $\mu$m steps. Additionally, two multifibre models have been analyzed with random choice of the investigated parameters.

Figure 3 shows for example the computed curves of the indenter force over the displacement for the study on fibre density.

The range of the varied geometrical parameters leads to the band width of the particular peaks of reaction forces given in Figure 4.

Fig. 3: Computed force-displacement-curves for the study of fibre density.

Fig. 4: Comparison of the peak force ranges for the examined cases of fibre topology (left to right) die slot distance, fibre inclination, two fibre models (orthogonal/parallel fibres), fibre clustering, random multifibre models.

4. Conclusion

The results show that the influence of the fibre topology in the specimen on the peak force is found in a range of 10%.

Mainly the fibre position related to the die slot and the fibre density influence the deviation of the peak force. By comparing the results of the random multifibre models it can be assumed that the experimental results for a real microstructure of a long fibre reinforced thermoplastic should not exceed this range.

References
COMPUTER TOMOGRAPHY AND IMAGE PROCESSING FOR ANISOTROPIC DAMAGE DETECTION ON FIBRE REINFORCED PLASTICS

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1. Introduction

Volume computer tomography (VCT) is an established non destructive testing method, which offers the possibility to capture internal structural damage three-dimensionally [1]. Thus, the complex failure mechanism of endless fibre reinforced plastics can be visualized and classified into the basis failure modes: fibre failure (FF) and inter fibre failure (IFF) taking different loading types into account: tension, compression and shear [2]. A new approach analyzes these VCT data in combination with image processing algorithms to identify the anisotropic damage parameters of pre-damaged CFRP tubes in dependence of the real arising failure modes. Thereby the kind, size and orientation of the detected failure will be estimated automatically. The software was developed on basis of the MATLAB environment and provides input data for a subsequently finite element analysis in ANSYS.

2. Methods

Materials and damage initiation

Tubular structures examined in this study were all manufactured using the filament winding process. The cylinders have an internal diameter of 32 mm and a wall thickness of 3.2 mm consisting of a [±α₄]₄S stacking sequence lay-up. Samples for testing are 280 mm long and were damaged by using a pendulum impact test machine. The incident impact energy was controlled to low magnitude, so that the sample was not penetrated and only internal or barely visible damage occurred.

Computer tomography, data acquisition

The CT investigation was performed on pre-damaged CFRP tubes by using a cone-beam tomography consisting of a 225 kV microfocus X-ray source, an object slide and a 2048 x 2048 pixel flat panel detector. Due to the relationship of voxel resolution and object size the examined range was limited to a volume of about 36 mm x 36 mm x 28 mm (1600 projections per scan, scanning time ~72 min). So the reconstructed data has a resolution of 17.53 µm/voxel. The commercially available software (Volume Player Plus) has generated 2D-slices in x,y-plane with a series of 1579 images in z-direction (see Fig. 1).

Fig. 1: a), b), c) 3D volume 3D-micro-CT reconstruction; d), e), f) cross sectional slice (2D reconstruction); g) slice with selected areas for manual analysis.

Automated analysis of CT-data

Most commercial software packages for CT data have limited capabilities for extracting quantitative data characterising the reconstructed structure [2]. To investigate localised damage it is important to extract information like distributions, size and orientation of the damaged structures. Because of noise and variations in intensity of 3D grey value data structure, the individual crack can’t be easily identified from the whole CT-dataset. This paper presents a systematic approach to obtain this kind of information, using a special software tool created in the MATLAB environment. The algorithm of the program (Fig. 2) includes chosen Digital Image Processing operations and focuses on an analysis of a large number of slices of the inspected specimen by utilizing quick and effective techniques. The newly-developed CT-based Damage Analysis System (CT-DAS) uses a sequence of slices,
which were extracted from VCT data in x, y-plane and 256 grey levels, as input images. According to the introduced algorithm CT-DAS finds, locates and classifies internal damage structure. The extracted information can be used for a finite element analysis.

3. Results and Discussion

Volume Computer Tomography (VCT) generates large data for the investigation of internal structure damages. The manual observation of these 3D data is time-consuming and leads to subjective results. Therefore a systematic approach to obtain an automated analysis method for large VCT data was developed on basis of 2D digital image processing. By processing the 2D images the extraction of damage distribution, size and orientation motivated by CUNTZE’s fracture-mode-concept [2] was focused. With the acquired VCT data it is possible to classify three different fracture types corresponding to fibre reinforced materials: i) delamination or inter fibre fracture (IFF), ii) fibre fracture (FF) and iii) pores and local inhomogeneity (Fig. 3). The possibility to separate these kinds of damage opens new opportunities to investigate them and the interactions between them. This detailed description of the object structure leads to the prediction of damage parameters and can be mapped to a finite element mesh (Fig. 4).

For detection and classification of existing structure damage within FRP components a software was developed on the basis of image processing algorithms using the MATLAB environment. An evaluation of the local damage and thus the FRP degradation can be made by such an identification of the failure modes, which can flow into appropriate finite element models. With the following global FE-analysis in combination with the implemented failure-mode-based "degradation elements" the remaining mechanical behaviour of the composite structure can be determined concerning stiffness and strength.

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References

IMPROVEMENTS OF AN ANALYSIS TOOL FOR THE PORE SIZE DISTRIBUTION ASSESSMENT

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1. Introduction

Knowledge of the inner structure is an important prerequisite for estimation of the mechanical properties of heterogeneous materials. This article is focused on the assessment of pore size distribution of a porous material.

2. Investigated materials

As representatives of biological materials specimens of human and porcine trabecular bone were tested (both harvested from region of proximal femur), as representatives of the artificial material synthetic pumice and frit glass were used.

3. Image acquisition

Three different devices (confocal microscope, CCD camera and flatbed scanner) were used for image acquisition.

Confocal Laser Scanning Microscope LEXT OLS3000 (Olympus Inc., Japan) performs the reconstruction of the scanned surface using a laser beam with magnification up to 1500× and resolution higher than 0.1μm. Results of the scanning are represented by a 2D matrix of heights and high resolution images (1024×768px). Physical size of the scanned area is 1280×960μm with minimal magnification (120×). This device is suitable for micro-porosity estimation. Obtained data are easily processable. The only limitation of this device is the small scanning area in case of macro-porosity.

Using a CCD camera CCD-1300F (VDS Voskühler GmbH, Germany) with resolution 1280×1024px attached to an optical microscope (Navitar Imaging Inc., USA) allows to capture 8-bit colour depth images with up to 24× magnification. The advantage of this method consists in continual magnification setup (3-24×), but the image quality is very sensitive to illumination conditions.

In the third case, the images of the samples were acquired by a high resolution flatbed scanner EPSON Perfection V350 (Seiko Epson Corporation, Japan). Maximal resolution 4800dpi was used (1px corresponds to 5μm) with 16-bit colour depth. Physical size of the scanned area is up to 210×297mm. This method is suitable for materials with larger pores, because samples with dimensions corresponding to representative area were required to register enough wide range of voids.

4. Image analysis

The image analysis procedure consists of two main steps: image segmentation and connected component analysis. Both parts of the procedure were implemented in Matlab. For the image segmentation thresholding was performed to convert captured grayscale images into binary ones and morphologic operations (opening and closing) were used to separate connected cross-section of voids. To estimate the area of voids two-pass algorithm for connected component analysis was used. This algorithm allows to estimate size of the recognized objects and other characteristics e.g. perimeter or orientation.

5. Stereological calculation

For the pore size distribution assessment based on the sizes of cross-sections estimated from two-dimensional data a stereological calculation technique was used. A method introduced by Xu & Pitot based on geometric properties of a sphere was chosen and implemented as a Matlab
function. Pore sizes obtained by 2-D image analysis were arranged into 25 size classes and the pore size distribution was estimated.

6. Results and conclusions

Using described analysis tool pore size distribution curves were obtained for three specimens of each material listed in section 2. These curves are depicted in Fig. 1 (biologic materials) and Fig. 2 (artificial materials).

![Fig. 1: Pore size distribution of biologic materials distribution obtained by image analysis](image1)

![Fig. 2: Pore size distribution of artificial obtained by image analysis](image2)

Three distinct approaches to acquire image data were tested. Comparison of properties of the used image acquisition techniques is listed in Tab. I.

<table>
<thead>
<tr>
<th>Image acquisition method</th>
<th>Lower size limit of registered pore [μm]</th>
<th>Maximal samples dimensions [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Confocal microscope</td>
<td>1</td>
<td>0.64×0.48</td>
</tr>
<tr>
<td>CCD camera</td>
<td>5</td>
<td>18×12</td>
</tr>
<tr>
<td>Flatbed scanner</td>
<td>20</td>
<td>210×297</td>
</tr>
</tbody>
</table>

Obtained results show possibilities and limitations of the presented analysis tool. In case of well chosen image acquisition method voids in required size range can be registered. The range is also limited by the used stereological calculation, ratio between the lower and upper size limit of registered pores is 250 (determined by number of size classes and scale factor between the size classes).

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References

THE INFLUENCE OF ADHESION ON THE STATE OF THE SURFACE LAYER OF CONTACT LENSES

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1. Introduction

Several research focuses on assessing the properties of contact lenses, due to the increasing number of users, whose needs grow because of the conditions in which they work (work at the computer room air-conditioned). The main aspect of providing comfortable wearing contact lenses is their good wettability. Studies on wettability are evolving all the time because of the difficulties to be faced, e.g. incomplete blinking. Hence the search for closest to the natural conditions of the test method, which reflects the reaction of the cornea-contact lens-tear film.

In order to understand the mechanism of interaction between different elements of the system discussed, there need to refer to the fundamental rights of the interactions at the interface between different phases.

Adhesion of a liquid to a solid phase is a physicochemical process, which involves tying up each of these two phases as a result of intermolecular interactions [1]. Wettability of the test body depends on the surface tension of the liquid present in the system, and the hydrophilicity of the surface [2,3]. However, mostly used to determine the wettability, is contact angle measurement [1].

The main purpose of this study was to investigate various soft contact lens care solution adhesion to the surface of different types of contact lenses. In addition to standard contact angle measurements using goniometer, a modified method for hydrophilicity measurement of contact lenses, was applied.

2. Modified Method

With respect to interactions on the surface of contact lenses can be derived dependence of the surface tension and energy free surface of the solid (Fig. 1).

Fig. 1: The relationship between surface tension of liquid droplets and the energy free surface of solid

Reffiring to the above mentioned dependencies, it is possible to determine the surface wettability by setting the contact angle, using a goniometer and the sitting drop method (Fig. 2)

Fig. 2: Droplet deposition onto the surface of the contact lens

Determination of contact angle in the sitting drop method involves the use of appropriate trigonometric dependence. Nevertheless due to the interactions between surfaces of different phases (liquid absorption or evaporation), which can result in a change of contact angle over time, other methods are developed.

The method of detachment of solid from the liquid using torsion balance gives an opportunity to study the hydrophilicity of the solid surface. Where hydrophilicity of each material determines its ability to attract water molecules, which in the case of polymeric materials largely depends on their structure [3]. Determination of hydrophilicity is based on measuring the force needed to detach from the surface of the test fluid measurement after the creation of the meniscus. In order to adjust the detachment method for measuring the hydrophilicity of contact lenses, the handle of an appropriate geometry was made (Fig. 3)
3. Research Materials and Measurements

In the present study, contact lenses from different manufacturers were used, with the same optical power and similar geometric parameters. In addition, were tested solutions, which are recommended by the contact lens manufacturers.

Tab.1: Soft contact lenses – characteristic

<table>
<thead>
<tr>
<th>Type of material</th>
<th>Water content</th>
<th>Buffer</th>
<th>Diameter</th>
<th>Radius of curvature</th>
<th>Producer</th>
</tr>
</thead>
<tbody>
<tr>
<td>Nallycon A</td>
<td>60.0%</td>
<td>Buffered saline and 0.05% Polymethan</td>
<td>14.0</td>
<td>8.7</td>
<td>Ciba Vision</td>
</tr>
<tr>
<td>Nallycon A</td>
<td>46.0%</td>
<td>Buffered sodium chloride solution of methyl cellulose</td>
<td>14.2</td>
<td>8.5</td>
<td>Johnson &amp; Johnson</td>
</tr>
<tr>
<td>Hilsiten B</td>
<td>59.0%</td>
<td>Sodium solution containing a basic buffer</td>
<td>14.2</td>
<td>8.6</td>
<td>Bausch &amp; Lomb</td>
</tr>
</tbody>
</table>

Characteristics of contact lenses used in the study is presented in Table 1, while in Table 2 is shown, the characteristics of the two selected and used for research - soft contact lens solutions.

Tab. 2: Soft contact lenses solutions - Parameters

<table>
<thead>
<tr>
<th>Name of solution</th>
<th>Preservative</th>
<th>Cluening agent</th>
<th>Moisturizing agent</th>
<th>pH</th>
<th>Osmolarity [mol/L]</th>
<th>Surface tension [mN/m]</th>
<th>Viscosity [cP]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Solocare Aqueous</td>
<td>Polysorbate 80</td>
<td>Polyethylene glycol</td>
<td>Disodium EDTA</td>
<td>7.23</td>
<td>3.10</td>
<td>35.1</td>
<td>1.15</td>
</tr>
<tr>
<td>Optiflex Aqueous</td>
<td>Sodium iodide</td>
<td>Sodium polycarboxylate</td>
<td>5.82</td>
<td>128</td>
<td>21.2</td>
<td>1.04</td>
<td></td>
</tr>
</tbody>
</table>

To obtain best contact lens adhesion to holder, element with smaller diameter was used. Data acquisition is shown on Figure 4.

Fig. 4: Contact lens detachment from solution

Prior relevant research, carried out a number of important, preliminary measurements to optimize research methods.

4. Experimental Results

The following submitted Figure 5 illustrates the change contact angle of soft contact lenses, depending on the used soft contact lens care solution. For comparison in the chart, are shown results of the contact angle formed, at the contact lens system with distilled water.

Fig. 5: Effect different solutions on the wettability of soft contact lenses

All tested soft contact lenses care solution are contact lenses well-wetting substance. Figure 6 shows the hydrophilic properties of contact lens against different soft contact lens care solutions.

Fig. 6: Effect of care solutions on the hydrophilicity of soft contact lenses

The hydrophilicity of the contact lens changes, depending on the type of care solution.

Due to reflect actual conditions in a modified method of hydrophilicity measurement and results of research are similar to results obtained by other investigator, this method may soon replace the standard contact angle tests [2,3].

5. References

COMPARATIVE INVESTIGATION OF STATIC FRICTION COEFFICIENT OF 6082 ALUMINIUM ALLOY WITH THE MATERIAL CONDITIONS T651 AND EQUAL-CHANNEL ANGULAR PRESSING (ECAP)

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1. Introduction

Knowledge of occurring friction is very important in mechanical engineering – both in order to ensure reliable friction-locked joints and to reduce energy loss. Friction coefficients are influenced by many factors, like hardness, material strength and structural conditions [1]. These materials properties can be improved through ultrafine graining of aluminium alloys by equal-channel angular pressing (ECAP) with subsequent aging treatment [2]. Therefore the influence of ECAP material conditions on the frictional behaviour of 6082 aluminium alloy is investigated in this paper.

2. Experimental Investigation

The test bench for determination of friction coefficients is shown in fig. 1.

Within the friction investigation specimens of 6082 aluminium alloy with temper designation T651 in comparison to those of ultrafine grained material condition by one ECAP pass with subsequent optimized aging treatment – called ECAP optimized – were used. Due to the material treatment an increase in the Brinell hardness of about 22 % and in the yield strength of approximately 8 % was reached. These results were achieved in collaboration with the Institute of Materials Science and Engineering (Chemnitz University of Technology) and are in accordance with earlier observations in [2].

Besides of the material properties the geometrical surface characteristic is important for the results synthesis. However usual parameters for surface roughness like average surface roughness Rz and mean roughness index Ra are not suitable, because of insufficient description regarding to the real surface profile shape [3]. Therefore S parameters are used, which refer to the surface instead of the profile (R parameters). The Sa parameter includes information about the surfaces roughness height and the Ssk parameter about the skewness of the amplitude distribution curve of the surface components. A negative Ssk value means the maximum of amplitude distribution is above the centre line (fig. 3). It mirrors the structural behaviour of the surface.
because there are only small peaks i.e. plateau shaped. A Ssk value of zero is reached by symmetric distributed surface components.

Fig. 3: Representation of a negative skewness (Ssk), according to [3].

3. Results and Discussion

The manufacturing process of all specimens was identical. The functional i.e. the frictional surface was made by conventional final cutting using constant process parameters. In spite of this specification differences in the surface geometry between both material conditions can be observed. The measured Ssk values are nearly zero. They tend mainly to be negative for specimens with temper designation T651 and positive for the ECAP optimized ones. Latter ones have a rougher surface, how the higher Sa values show in tab. 1.

The surface modification in consequence of the frictional test was independent from the material condition. Following differences were observed between the lubrication conditions. The skewness value Ssk of degreased specimens became positive after the test and the Sa values increased. In contrary, lubricated specimens have reached a negative Ssk value and a slightly decreased Sa value. This means that the peaks are levelled. (tab. 1)

The recorded torque-twisting-angle-diagrams and so the determined friction coefficients do not differ significantly between the two investigated material conditions, particularly in regard to small differences in surface conditions and test scatter. However there are large differences between the two lubrication conditions (fig 4).

Fig. 4: Torque-twisting-angle-diagram (degreased and lubricated).

As a comparison value the maximum friction coefficient $\mu_{max}$ was used, which is defined as a local torque maximum within 0.4° twisting angle. This value decreases over 40 % for lubricated contact in comparison with the degreased. (tab. 2)

<table>
<thead>
<tr>
<th>Test condition</th>
<th>T651 before → afterward test</th>
<th>ECAP optimized before → afterward test</th>
</tr>
</thead>
<tbody>
<tr>
<td>degreased</td>
<td>Sa: 0.32µm → 1.48µm</td>
<td>Sa: 0.81µm → 2.96µm</td>
</tr>
<tr>
<td></td>
<td>Ssk: -0.17 → 0.34</td>
<td>Ssk: 0.10 → 0.96</td>
</tr>
<tr>
<td>lubricated</td>
<td>Sa: 0.36µm → 0.32µm</td>
<td>Sa: 0.57µm → 0.51µm</td>
</tr>
<tr>
<td></td>
<td>Ssk: -0.01 → -0.29</td>
<td>Ssk: 0.13 → 0.18</td>
</tr>
</tbody>
</table>

Acknowledgements: The authors express their thanks to the German Research Foundation (DFG) for financial support of the Collaborative Research Center (SFB) 692 and the subprojects C1 and T2 for providing the ECAP material.

References

EXPERIMENTAL CHARACTERIZATION OF MAGNETOElastomers AND DETERMINATION OF MATERIAL MODEL PARAMETERS FOR SIMULATIONS

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1. Introduction

To assess the applicability of magnetic metallic particle filled soft polymeric materials (i.e. gels and elastomers, termed as magnetoelastomers) for practical industrial applications, comprehensive characterization of the rheological and mechanical behaviour is essential.

For this study Polydimethylsiloxane (PDMS) was selected as matrix-material. Iron particles of various shapes (aspect ratios) and surface treatments were added as fillers in various amounts. Isotropic specimens were prepared and subjected to rheological and dynamic mechanical measurements in presence or absence of homogeneous magnetic fields. In order to achieve the necessary field homogeneity in the region of the test specimen, novel set-ups were developed using magnetostatic simulation software and rapid prototyping tools.

2. Experimental Setup – Design and Development

The elastomer specimens were tested with a BOSE Electroforce test bench. To allow for experiments in homogeneous magnetic fields as well, a novel setup had to be designed, that could be used in conjunction with the existing system. To keep it small and easy to use, it was decided to generate the field by installing two NdFeB permanent magnets, one on each side of the specimen. The optimal magnet dimensions for achieving maximum field homogeneity at a given distance (specimen size plus some space for clamping and loading strain) were calculated using magnetostatic simulation software (Maxwell). For two NdFeB disks of 15 cm diameter and 1 cm thickness at a distance of 7 cm, a quasi homogeneous field of 103.5 mT in the specimen region is predicted (fig. 1).

This was checked experimentally via teslameter measurements, the deviations were indeed less than 1% with an average flux density of 117 mT. The jackets for attaching the magnets onto the test bench and the specimen clamps were designed in CAD software, then printed with a Stratasys FDM system (fig. 2).

Additionally a steel frame was constructed to bring the test bench into an upright position, thus avoiding any bending force acting upon the load cell. The finished experimental setup is displayed in figure 3.
3. Specimen Preparation

For this study PDMS was compounded with different types of iron particles. The following parameters were varied:

- cross link density of the PDMS matrix (various amounts of X-linker added)
- filler content
- particle shape (aspect ratio, AR)
- particle surface treatment (unmodified, silica coating, silanized SiO₂ coating)

From these mixtures cylindrical specimens were prepared using casting moulds made of PTFE and cured for 24 h at room temperature.

4. Experimental Results

The dynamic mechanical behaviour of PDMS specimens containing particles with various surface treatments (uncoated, SiO₂ coating, silanized SiO₂ coating) was investigated.

While the uncoated particles gave the expected increase in stiffness (80% up), the surface treated particles caused the PDMS-Matrix to soften drastically (SiO₂: 92.5% down, SiO₂/Silane: 74% down).

Furthermore the stiffness change caused by a magnetic field aligned in loading direction was studied (fig. 5). This effect can be quantified through the magnetic stiffening factor (MSF) [1].

\[ MSF = \frac{E_{MF}}{E_0} \]

Fig. 5: Storage modulus and MSF of PDMS with various X-linker conc. and 10% Fe (uncoated, AR1)

The application of a magnetic field increased the stiffness of all specimens, at which an inverse correlation between matrix-stiffness and MSF was found. For the very soft elastomers containing the coated particles, this effect was even more pronounced (up to 300% increase).

Future work will focus on the development of a procedure for processing particle filled elastomers in magnetic fields to achieve particle distributions of controlled anisotropy as well as compounding magnetic particles with different elastomer matrices (Acrylic elastomers, Engage type thermoplastic elastomers) to cover a wider range of viscoelastic properties.

References

DEVELOPMENT OF A TRIBOLOGICAL TESTING METHODOLOGY FOR DETERMINING FRICTION AND WEAR RELATED PROPERTIES OF TPU SEALING MATERIALS

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1. Introduction
Mechanical seal performance critically depends on the tribological performance of the sealing material [1]. Former research work clearly indicates that the in service behaviour of TPU seals is associated with the sliding velocity (v) and the contact pressure (p) [2], hence a compelling testing process to determine and compare the friction and wear related properties based on pv-variations is of outermost importance. This paper chronicles an implementation approach for the development of a testing methodology capable to declare the pv-dependency on the tribological performance.

2. Experimental - Methodology
One filled (Shore A/D=95/48) and one unfilled (Shore A/D=95/47) TPU-material representing general sealing materials are investigated. A testing process is set-up demonstrating the influence of variations in pv-parameters on the materials tribological behaviour. Therefore two methods are developed, a load step test at constant speed and a speed step test at constant loads. The validity of the test results are approved by analyzing the coefficient of friction (COF) and the correlating frictional heat build up which respond significantly to the pv-steps. Based on those results, the step tests are performed by progressive stages. After every load/speed step the tribological behaviour of the specimen is evaluated and the formation and evolution of the contact surfaces are analyzed via micrographs (light microscopy, SEM). The measured data is compared to the worn surface micrographs to assess the pv-dependency in more detail. Further thermomechanical analysis of the materials is performed to approve the supposed pv-rating.

3. Experimental Setup and Performance
The tribological tests are performed at the precise rotary tribometer TE 93 (Phoenix Tribology Ltd., UK). The RoD (Ring on Disc, Fig. 1) testing device, based on ASTM D3702 [3] for metallic materials, has been adapted to polymeric materials during our former research [2, 4]. A load cell measures the torque and two thermoelements measure the near surface temperatures due to frictional phenomenons at two positions in the counterpart. The loss of mass of the polymer is consulted to analyze the wear. Conclusively, the setting parameters (normal load, speed) are held constant or varied in steps during the tests, while friction and wear related parameters (temperatures, frictional torque) are measured.

![Fig. 1: RoD-Testing (schematically)](attachment:image)

4. Experimental Results
The tested materials differ significantly in their tribological behaviour. The unfilled material shows high wear and failure occurs at low pv-levels (Fig.3). The filled material is more immune to tribological load which results in low wear. The load/speed step tests show a strong dependency of the frictional parameters on pv-variations. Figure 2 demonstrates the COF and the near surface temperature for selected pv-levels for the filled and unfilled TPU material.

The COF decreases both with increasing load and speed due to friction heat accumulation and changes in the contact geometry.
Figure 2: Frictional behaviour of TPU materials at selected pv-levels

Figure 3 shows an example of the development of the COF and the near surface temperatures dependent on the testing speed for both materials at a constant load of 1 MPa.

Figure 3: Speed step test at 1 MPa, a) TPU filled, b) TPU unfilled

The temperature erratically increases with the speed steps. The friction force decreases on account of the development of tribologically more stable surface contact conditions and thermomechanical effects like bulk softening. Dynamical mechanical analysis of the tested materials show bulk softening (loss in modulus) at elevated temperatures of 150°C [5]. Due to the measured temperatures, the same level is assumed for the real contact temperature (Fig. 3). Optical micrographs (Fig. 4) approve the altering contact conditions for the speed steps. Wear particles and a rather coarse surface structure indicate higher wear (Fig. 4b) and failure mechanisms for the unfilled material. In comparison, a lubricating or bearing structure signifying stable conditions and lower wear (Fig. 4a) of filler particles is structured for the filled material.

5. Conclusion

The step tests are proved of value as appropriate methodology to rate the pv-dependency of tribological parameters of TPU. It is an approach to formulate a pv-application limit for sealing materials. Additionally, wear mechanisms can be investigated via precise analysis of the surface characteristics.

Acknowledgements: The research work of this paper was performed at the Polymer Competence Center Leoben GmbH (PCCL, Austria) within the framework of the COMET-program of the Austrian Ministry of Traffic, Innovation and Technology with contributions by the Chair of Mechanical Engineering, University of Leoben, and SKF Economos GmbH. The PCCL is funded by the Austrian Government and the State Governments of Styria and Upper Austria.

References

SYMPOSIUM SESSION II
ESTIMATION METHOD OF SPOT CONNECTORS 
MECHANICAL PROPERTIES

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1. Introduction

Clinch joining technique can be successfully used in many industries for joining of thin-walled profiles. This solution brings multiple benefits to the producers. The most important are: elimination of metal surface preparation, reduction of the energy consumption and negative influence of the heat zone. Furthermore, joining process does not cause bore dust, and does not require gas shroud, generating noise at a low level of intensity [1].

The advantages of the clinch connections tend leading European tools manufacturers like Attexor Equipments, BTM, Eckold, TOX, or Trumpf to use them more and more frequent. At the same time intensification of studies concerning their mechanical properties can be observed. The authors of papers [2, 3] focused on the issue of clinches suitability for joining steel parts. They analyzed carefully the advantages and disadvantages of this technique and the process itself. One of the most interesting paper focused on this subject is [4], in which a comparison of clich, bolt and rivet connections in terms of their strength and usability in steel constructions was made. Another interesting paper is [5]. It is focused on the impact of SPJ (square press joints) tensile force direction on connection shear strength. Paper [6] is focused on degradation of the press joints mechanical properties under the influence of time. You can also meet with the papers of the author [7], which examines the impact of spot connections quantity on the thin-walled elements mass-production costs. There is a lack of comprehensive studies concerning clinch joints mechanical properties, especially about their minimum load-carrying ability, which ensure correct deformation process for stroke-loaded elements.

2. Material tests

Material tests were made for currently used in automotive industry, typical, higher-strength steels, such as: DP600, DP800, DP1000, DP1200 and deep-drawing steel. For specified grades of steel static and dynamic material tests were made. Next step was definition of the material constitutive equations according to the Cowper-Symonds model:

$$\sigma_n = \sigma_y \left[ 1 + \left( \frac{\varepsilon}{D} \right)^{\frac{1}{p}} \right]$$

where:

- $\sigma_n$ - stress level due to the material strain rate hardening
- $\sigma_y$ - static stress level
- $\varepsilon$ - strain
- $\dot{\varepsilon}$ - strain rate
- $\gamma, \epsilon_u, A, B, p, D, D_u, D_y$ - other parameters to calculate

Next step was definition of the material constitutive equations according to the Cowper-Symonds model:

$$\sigma_n = \sigma_y \left[ 1 + \left( \frac{\varepsilon}{D} \right)^{\frac{1}{p}} \right]$$

Next step was definition of the material constitutive equations according to the Cowper-Symonds model:

$$\sigma_n = \sigma_y \left[ 1 + \left( \frac{\varepsilon}{D} \right)^{\frac{1}{p}} \right]$$

The best correlation between the model and obtained results $D$ and $p$ coefficients were determined by means of the statistical analysis tools. The figure below depicts correlation between the Cowper-Symonds model and one of the material analysis results - i.e. DP600 higher-strength steel.

![Fig. 1: The correlation of the Cowper-Symonds strain rate hardening model and DP600 steel grade](image)

3. Connection properties

Obtained $D$ and $p$ factors were used in the material numerical models definition. In order to conduct numerical experiments, geometric models of car longitudinal were made. Top-hat and double-hat profiles were connected by means of separable joints with the following failure criterion:
\[
\left( \frac{F_N}{F_{N\text{max}}} \right)^{a_1} + \left( \frac{F_S}{F_{S\text{max}}} \right)^{a_2} \leq 1
\]
where:
- \(F_N\) - normal force value,
- \(F_{N\text{max}}\) - normal force maximum value,
- \(F_S\) - shear force value,
- \(F_{S\text{max}}\) - shear force maximum value.

Used connection stays integral as long as above relation is a true. It should be noticed that, depending on the different values of \(a_1\) and \(a_2\) coefficients, connection is characterized by different load-carrying ability:

For further test, the coefficients \(a_1\) and \(a_2\) were set to 1, and the values of maximum forces \(F_{N\text{max}}, F_{S\text{max}}\) were set to 50kN. Implementation of joints with higher strength properties will therefore ensure that the joined profiles will not be disconnected during dynamic deformation. The fact that the described methodology may find application in the estimation of minimum mechanical properties of other types of spot connectors, such as welds, is noteworthy.

4. Conclusion

Analysis of joint normal and shear forces allows to determine their maximum value during deformation of the sample. Analysis of these data allow to draw a conclusion, that the developed research methodology enable to determine spot connection minimum load-carrying ability which ensure correct crush parameters such as shortening of the sample and amount of absorbed energy.

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ANALYSIS OF TECHNICAL RUBBER MATERIALS USING SIMPLE SHEAR DEFORMATIONS WITH ROTATING AXES

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1. Introduction

In opposition to metallic materials, technical elastomers exhibit a variety of different material properties like nonlinearity or inelastic material behavior. The measurement of those properties and the determination of the material parameters connected to them often requires several different experimental setups. In the presented article the setup of an experimental rig is introduced, which allows the measurement of dynamic parameters, while at the same time the input signals preserve a quasi-static character. With the experimental rig it is possible to decouple elastic and inelastic processes within the material as well as to investigate consider new and yet highly relevant aspects for the prediction of the fatigue behavior of technical rubber parts.

2. Experimental Setup

The following sketches show the schematics of a simple shear deformation with rotating axes. In this context it has to be mentioned, that in order to describe simple shear, it is not sufficient to consider the deformation state in respect to a reference configuration only. In fact simple shear is rather a deformation process than a deformation state, which has to be regarded in dependency of the progression of the shear measure \( s \) to consider the essential properties like for example the change of the direction of the eigenvectors of the load in the coordinates connected to the material. A simple shear deformation process with rotating axes can be divided into two phases, as is shown in the figure above. During the first phase a simple shear deformation is initiated in a material point, which is represented in the figure via a cube. Throughout the second phase the plane parallel to the base area is moved translatorically on a circular track around its original position. This is done in such a manner that all planes of the cube stay coplanar to each other. The reaction force of the deformation process is the measured quantity of the test machine. As shown in the figure the reaction force is not collinear to the direction of the deformation. Therefore it is possible to split the force into one fraction parallel to the simple shear direction and one fraction in circumferential direction. The partial force in shear direction determines the amount of the...
elastoplastic portion of the energy-balance and the partial force in circumferential direction the dissipated amount of the energy.

3. Layout of Experimental Rig

For the realization of a simple shear process with rotating axes an experimental setup on the basis of the apparatus proposed by Gent in [1] has been implemented. Figure 1 shows the principle of the proposed test machine.

The test rig consists of three bearing cases and two samples in double-sandwich-arrangement. Both outer bearing cases are fixed in their position, the central bearing case has a translatoric degree of freedom perpendicular to the axis of the bearings. Each of the two samples is mounted between one outer and the central bearing case. By means of a displacement of the central bearing case a simple shear deformation is initiated in the samples. By applying a torque at one of the outer bearing cases a simple shear deformation with rotating axes is put into practice. By mounting two force sensors in a suitable position, both components of the reaction force, one in the direction of the shear deformation and one in circumferential direction, can be measured separately. The measurement of these two force components allows a characterization of technical rubber materials concerning their different properties.

4. Experimental Results – Mechanical Characterization

The mechanical properties of technical used elastomers show a profound dependency on the loading history and the loading amplitude. With the help of the test machine for the realization of a simple shear deformation with rotating axes, it is now possible to measure the stress-induced softening of carbon black filled elastomers in respect to time and loading amplitude. Fundamental properties like shear stiffness and loss-angle are also measurable without large operating expenses in real-time.

5. Experimental Results – Fatigue Measurements

Besides the analysis of the mechanical properties of filled elastomers in the temporal close-up-range, it is also desirable to be able to predict the lifetime behaviour of industrial produced rubber parts. Regarding this background the experimental rig offers the possibility to investigate new and not yet considered aspects to determine and predict the fatigue behaviour of technical rubber materials. Common fatigue studies of rubber parts mainly deal with the variation of the loading amplitude. In addition to those investigations, simple shear deformations with rotating axes show the influence of changing loading directions onto the fatigue resistance of rubber; an aspect, which has not been considered yet in the prediction of the lifetime of rubber parts.

6. Summary

The presented experimental setup enables to analyze new and not yet considered mechanical aspects of rubberlike materials. Especially in respect to the prediction of the lifetime of rubber parts, new and highly relevant features can be shown with the help of simple shear with rotating axes, which enables to create advanced models for the simulative prediction of fatigue behaviour of rubber parts.

References

STUDY OF DEGRADATION OF FIBRE – CEMENT PLATES WITH DIFFERENT TYPES OF NON – METALIC FIBRES

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1. Introduction
Results from series of tests carried out on nine mixture formulas are the main data set. Asbestos-free plates bonded by hydro-silicate matrix and reinforced by organic fibers were produced from these mixture formulas. Within the scope of this project, the following activities were carried out: finding out the dimensional and weight characteristics of the plates, bending strength under normal conditions, the influence of frost on bending strength, the influence of putting the plates into hot water on bending strength, the impermeability of plates and measurement of change of length in the direction along and across the fibers.

2. Test Devices – Test description
All of the following tests in addition to shrinkage were performed according to standard ČSN EN 12467 [1]. The following tests in addition to shrinkage were performed using to the Klokner Institute method. Three-point bending test (fig. 1) was performed on the test device Instron 250 kN. Each board was tested in two directions: parallel of fibers and perpendicular to the direction of fibers. Test boards were burdened so that a rupture occurred in 10 to 30 seconds, support distance was 200 mm. Before each test, the plates were measured and weighted.

3. The procedure of monitoring the hot water test
The testing plates were divided into two sets. After conditioning, the first set was tested for tensile strength when bending. The second set of testing plates was put into water with the temperature of 60 ± 2 °C for 56 days in climatic chamber. After this time and conditioning, the testing samples were tested for tensile strength when bending. The test was conducted according to the above – mentioned standard.

4. The procedure of monitoring the saturation – desiccation test
After conditioning, the first set was tested for tensile strength when bending. The second set was subjected to cycling – 18 hours water with the temperature 20 ± 2 °C and 6 hours of drying in a ventilated drying kiln, with the temperature 60 ± 2 °C. After the required number of 50 cycles, the testing samples were stored in a laboratory for 7 days with the laboratory conditions – temperature 20 ± 2 °C and humidity 50 – 60 %. After the conditioning of the second set, the bending test was conducted.

5. The procedure of monitoring the frost resistance test
The testing plates were divided into two sets. After conditioning, the first set was tested for
tensile strength when bending. The second set was subjected to cycling – 2 hours freezing in water (ice) with the temperature -20 ± 4 °C and 2 hours of defrosting in water with the temperature 20 ± 4 °C. The cycling was done automatically in a freezing chamber. After the required number of 100 cycles, the testing samples were conditioned. Subsequently, the bending test was carried out (fig. 2).

Fig. 2: Sample of results: The ratio of tensile strength when bending the testing samples (of both sets 1+2) direction across the fibers

6. The procedure of monitoring the impermeability test

The purpose of testing was to monitor the bottom surfaces of the testing plates, so that it would not be possible water to move them. The test took 24 hours. After this time, the bottom surface was checked.

7. The following tests in addition to shrinkage

The aims of the tests were comparison of material length change values in predefined settings: dried-up plate, real condition after placing in laboratory and air humidity 70%, temperature 20°C.

In the first step were measured dimensions and weight of plates. In the second step were stucked on into each corner of plate gauging disc through use of two-pack paste. These measuring points were marked A, B, C, D. Due to positions of discs were possible measured length change both in direction and in vertically direction of stiffening organic fibres in plates (fig. 3).

Fig. 3: Sample of results: Shrinkage of samples direction along the fibers

8. Experimental Results

Exclude the procedure of monitoring the hot water test the all others tests have very good results. We can see from measurements that the new selected recipes demonstrate more good results. Also – we can confirm that there are not big difference between tensile strengths of both sets.

The individual calculations, tables, individual graphs, photos from realization of the test and further documentation are available at the leader of the project because of the limited size of this article.

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References
DETERMINATION OF MECHANICAL PROPERTIES OF METAL FOAMS USING MICROSTRUCTURAL MODELS

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1. Introduction

Several different methods for determination of material characteristics have been developed as a result of efforts for cost reduction in cellular materials development and engineering use. This paper is aimed at the study of mechanical behaviour of aluminium metal foams by modeling their internal structure and analysis using finite element method. Firstly, the internal structure was modeled using voxel FE model created on the basis of series of CT scans. However, irregular cell shape and cell dimensions of selected reference material lead to huge and computationally demanding models with limited applicability. These considerations were motivation for subsequent discretization of the internal structure using Gibson-Ashby’s equivalence.

2. Geometrical and mechanical modeling

Fundamentals of Gibson-Ashby’s discretization are based on studies of beam deformation mechanisms considering that cell-wall bending is the principal mechanism of deformation of cellular materials. Deduction of macroscopic characteristics is thereby done by studying the bending of a beam, which represent the foam strut. This model was originally developed for discretization of open cell foams. In this study, the beam-only discretization is used intentionally to investigate its suitability for modeling of closed cell foams.

The elementary cell is considered to have hexagonal or cubic form (that is used in this paper), and the whole structure is then modeled by periodical network of elementary cells [1].

Overall material characteristics of the foam were derived from tensile loading of the structure. The analysis was performed in ANSYS software in both elastic and plastic fields. Cell network was generated using linear hexahedral elements with 24 degrees of freedom.

Aluminium closed cell foam Alporas was selected as a reference material. Cell parameters were chosen to match real material characteristics [2], behavior of geometrically isotropic and anisotropic elementary cells was studied according to typical production parameters, where spherical cells become polyhedron at porosities over 70 % . Alporas is manufactured using special unnormalized alloy containing 97 % of aluminium, 1.5 % of calcium and 1.5 % of titanium [3]. Because material properties of this alloy are not provided by the manufacturer, the material models use mechanical properties of 98 % aluminium as stated in [4]:

- \( E = 69 \text{ GPa} \)
- \( \mu = 0.33 \)
- yield strength \( \sigma_y = 300 \text{ MPa} \)
- tangential modulus \( E_t = 1.725 \text{ GPa} \)

Two variants of material model were used – linear elastic model and elasto-plastic model with von Mises plasticity and bilinear isotropic hardening. Material characteristics in both elastic and plastic fields were derived from displacement controlled tensional loading. Investigated relations were particularly the evolution of overall elastic modulus according to different relative densities and in the plastic field the tensional stress-strain diagram.

3. Results

Firstly the dimensions of representative volume element (RVE) were determined from...
evolution of elastic modulus vs. number of cells along each axis in the coordinate system. Substantial error was acquired at low number of cells and with growing number of cells the elastic modulus was decreasing. Cell network consisting of 12 cells along each axis was identified as RVE with equivalent real specimen dimensions 57.6 mm × 57.6 mm × 57.6 mm.

In the next step, parameters of the Gibson-Ashby’s cell were assessed. Mechanical response of beam-like discretisation highly depends on relative lengths of the connection stems. Analyses show that elastic modulus increase with shorter connection stems and in this paper 1/8 is considered as relevant value. Influence of cross-section shape was determined from comparison of rectangular and circular cross-section. Analysis of obtained relations indicate that the shape itself doesn’t have any influence on results for relative densities lower than 0.12, for higher relative densities the circular shape model becomes stiffer.

Essential part of the analysis was determination of relations between mechanical response and beam cross-section parameters. For the geometrically isotropic cell, published elastic modulus of Alporas foam was acquired with model relative density in interval 0.12 - 0.185. For the geometrically anisotropic cell, the declared properties of Alporas were acquired with model relative density 0.08 – 0.13, which is consistent with real mechanical characteristics.

Analysis in plastic field was concentrated at determination of tensional stress-strain diagram in direction \( x \). With respect to difficult convergence in geometrically nonlinear analysis, 10 % was selected as maximum strain value. Results were studied for geometrically linear or nonlinear simulation and also for statically definite or indefinite boundary conditions. Geometrically nonlinear simulation converged to real tensile behavior of Alporas with plateau of constant stress in extensive range of deformation as can be seen in Fig. 3.

4. Conclusion
This study shows that considered discretisation model can be used for numeric modeling of closed cell aluminum foams. Effects of beam parameters on total stiffness are highlighted and obtained results are in agreement with characteristics of real cellular materials. Increased length of connection stems decreases total stiffness, whereas increase in relative density increases total stiffness of the foam structure. Studies performed in plastic field are in agreement with plastic behavior of real specimens.

References
DETERMINATION OF PRESSURE DISTRIBUTIONS USING A GRADIENT BASED OPTIMIZATION METHOD AND APPLICATION AT FORMING TOOLS FOR HIGH GEARs

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1. Introduction

Gradient based optimization algorithms have found applications in many fields of science. In Mechanical Engineering it is common to identify unknown parameters of material models or to optimize the shape of structures. In this paper another application will be presented. Our research deals with the identification of the loads applied to a round rolling tool at a forming process basing on measured strains.

2. Round rolling of high gears

Round rolling is a favourable manufacturing process for helical gears (Fig. 1) if we compare it to cutting techniques. Very short processing time, no loss of material, no need for chip disposal and improved load capacity caused by contour-related fibre-orientation are only a few of its advantages.

3. Inverse strategy

The load identification we are presenting here is based on a fundamental assumption: We consider a linear elastic material behaviour of the forming tool. In this case the principle of superposition is valid.

Strain measurements $\mathbf{\varepsilon}_m$ are obtained from 20 strain gauges installed at a deformable measuring zone underneath the tool gearing (Fig. 2). Experimental setup and some experimental results are described in [2].

Fig. 1: Round rolling process

Fig. 2: a) original round rolling tool, b) FE-Model

We designed a numerical Model with a regular element structure (Software: MSC.MarcMentat, number of nodes: 280000, number of elements: 290000). This model has to reflect the behaviour of the real tool with high accuracy. Using this model we perform a large number of FEM calculations to estimate the sensitivity between each measuring point and all potential discrete contact surfaces at the tool gearing (Fig. 3).

Fig. 3: potential contact surface with load $F_n$ in normal direction and loads $F_{tu}$ and $F_{tv}$ in tangential directions

Thus we form matrices $\mathbf{C}_n$, $\mathbf{C}_{tu}$ and $\mathbf{C}_{tv}$ with calculated strains (in measuring direction) for each measuring point and three load directions (Fig. 3), where the rows indicate the u-location and the columns indicate the v-location of the potential discrete contact surface at the forming tool.

Another requirement represents an approach of the expected load distribution described as a function $f(\mathbf{p})$ with free parameters $\mathbf{p} = (\mathbf{q}, \mathbf{o})^{T}$. The load intensity is described with $\mathbf{q}$ and $\mathbf{o}$ is used to capture the load location. There will be more parameters if we consider the tangential loads too.
Having the matrices $C_{tn}$, $C_{tu}$ and $C_{tv}$, we can calculate strains $\varepsilon_c$ corresponding to $p$ at all measuring points (1).

$$\varepsilon_c = f(p, C_{tn}, C_{tu}, C_{tv})$$  \hspace{1cm} (1)

To find the unknown parameters, we need to minimize the least square between calculated and measured strains (2).

$$\Phi(p) = \|\varepsilon_m - \varepsilon_c(p)\| \rightarrow \min$$  \hspace{1cm} (2)

The in MATLAB implemented Levenberg-Marquardt algorithm is a convenient gradient based optimization tool to find a solution for this nonlinear inverse problem iteratively.

It’s obvious that we have to solve an ill-posed inverse problem, if we consider the validity of the Principle of St. Venant. For this reason we need to stabilize the optimization by applying a suitable regularisation. We introduce a regularisation term to penalize to large load intensities (3).

$$\Phi(p) = \|\varepsilon_m - \varepsilon_c(p)\| + \alpha \|q\| \rightarrow \min$$  \hspace{1cm} (3)

Fig. 3 shows the flow chart of the identification algorithm.

![Flow chart of the identification algorithm](image)

**Fig. 3:** Flow chart of the identification algorithm

### 4. Load identification

Let us illustrate the identification with a simple numerical example. We describe the expected load as shown in fig. 4 as piecewise linear function with three parameters for load intensity and two parameters for the u-location (v-location is constant). With this function we generate synthetic noisy measurements. Tangential loads are not considered. We start the identification with a chosen set of parameters. The first value of $\alpha$ is 1. To show the influence of $\alpha$, we perform 30 single identifications and half the value of $\alpha$ after each identification. Fig. 5 shows the results of this procedure. The significant discontinuity in the average change of the unknown parameters after step 20 indicates the best solution with a value of $\alpha=2 \cdot 10^{-6}$ at this step. The identified load matches very good with the applied load. If the influence of $\alpha$ is to big, the load distributes over a wide area of the tool gearing (Step 1). If $\alpha$ becomes to small (Step 25) we also can’t find a proper solution.

![Identification results](image)

**Fig. 5:** Identification results a) load distribution for selected values of $\alpha$, b) average changes of all parameters, c) Residual, d) horizontal force and e) torque for all values of $\alpha$ during the identification.

It has to be pointed out, that the selection of the starting parameters has also influence to the found solution. A variation of the starting parameters and comparison of the calculated residuals leads to the best selection.

### 5. Conclusions

The independence of the actual load identification from time consuming FEM-forward calculations represents an advantage if we do parameter studies at a single tool with fix geometric properties. Once the sensitivity between surface and measuring points is estimated, the presented method is a very fast way for load identification concerning linear elastic components.

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**References**


THE EXPERIMENTAL RESEARCH ON POLARIZATION OF TENSIONED REBARS COATED WITH SULPHUR POLYMER COMPOSITE

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1. Introduction

In order to demonstrate the suitability of sulphur polymer composites for the surface protection of concrete steel experimental research was carried out in the Institute of Building Engineering at Wrocław University of Technology. The research included: the experimental determination of sulphur polymer composite composition and manufacturing conditions, tests of the composite’s selected physical, chemical and mechanical properties, tests of its tangent and normal adhesion to plain and ribbed reinforcing bars and to standard cement mortar and concrete, the determination of the mass decrement resulting from storage in aqueous solutions of acids, hydroxides and salts and in water and the polarization investigation of rebars subjected to tension in a solution modelling the pore liquid in carbonated concrete contaminated with chloride ions.

2. Description of investigations

Polarization investigations of tensioned rebars coated with the sulphur polymer composite were carried out on plain St3S reinforcing steel samples immersed in a solution modelling the porous liquid in carbonated concrete contaminated with chloride ions. The samples were 10 mm in diameter and 290 mm long.

The results of the preliminary tests were analyzed and the sulphur polymer composite having the best properties among the tested composites was selected for further studies.

3. Test results and their analysis.

Polarization investigations of tensioned rebars coated with sulphur polymer composite

Investigation results have been obtained as standard computer diagrams with stationary potentials $E_0$ and corrosion currents $I_0$ indicated.

An overview of all corrosion current densities $i_0$, stationary potentials $E_0$, at loads $P$, tensile stresses $\sigma_0$ and corrosion rates $H_t$ is presented in [1, 2]. Corrosion rate $H_t$ has been calculated basing on current densities $i_0$ measured prior and with using a formula:

$$H_t = 1.123 \cdot k \cdot i_0$$  (1)

where: $k = 1.042$ g/ Ah means the electrochemical equivalent for iron.

Figure 1 shows corrosion rate $H_t$ versus time (in a time interval of 3-168 hours) at a constant rebar tensile stress $\sigma_a$ of 194.5 MPa. The rebars are plain rebars 10 mm in diameter, coated with a 0.5 mm and 1.5 mm thick layer of the sulphur polymer composite, immersed in a solution modelling the pore liquid in carbonated concrete contaminated with chloride ions.

Fig. 1: Corrosion rate versus time at constant tensile stress $\sigma_a$=194.5 MPa for plain rebars 10 mm in diameter, coated with sulphur polymer composite and for uncoated rebars.

According to the figure, after a tensile stress $(\sigma_a)$ of 194.5 MPa is reached, the corrosion rate $(H_t)$ changes in a time interval of 3-168 hours as follows: it increases initially and after 90 hours from the beginning of the test it starts decreasing, amounting to about 0.0010 mm/year after 168 hours. It is lower in the case of the rebars coated with a 1.5 mm thick layer of the sulphur polymer composite. At this layer thickness, the corrosion rate is only very slightly dependent on time and on the increasing tensile stress in the rebars. Within the test time interval it remained at an almost constant level of 0.000186- 0.000242 mm/year. As the figure shows, the corrosion rate for the uncoated rebars is by three orders of
magnitude higher. The corrosion rate over time is described by the equations given in fig. 1.

The very low, nearly constant corrosion rate in the case of the rebars coated with a 1.5 mm thick layer of the sulphur polymer composite is beneficial. Therefore such a layer can be considered as contributing to the protection of the reinforcing steel against corrosion in the solution modelling the porous liquid in carbonated concrete contaminated with chloride ions.

Figure 2 shows the dependence between stationary potential $E_o$, time and tensile strength for the tested rebars coated with a 0.5 mm and 1.5 mm thick layer of the sulphur polymer composite. For comparison purposes, stationary potential $E_o$ in similar uncoated rebars is shown. The dependencies are described by the included equations.

![Graph showing stationary potential versus time and constant tensile stress for rebars.](image)

**Fig. 2:** Stationary potential versus time and constant tensile stress $\sigma_a=194.5$ MPa for plain rebars

10 mm in diameter, coated with sulphur polymer composite and for uncoated rebars.

According to the test results, once tensile strength $\sigma_a$ of 194.5 MPa is reached in the rebars, a slight increase in stationary potential over time is observed. In the case of a 1.5 mm layer, potential $E_o$ remains constant (close to 0 mV) in the whole test period. It also remains constant for the uncoated reference rebars, but at a level much different from 0 mV.

4. Conclusion

It can be concluded from the test results that the tested sulphur polymer composite can provide surface corrosion protection to the reinforcing steel in concrete. Sulphur composites have not been applied for this purpose before.

The aim of investigation that has been led was to evaluate tendencies of the corrosion process for St35 reinforcing steel when covered with polymer sulphuric coating and exposed to tensile stress. Steel samples were loaded in a way that their yield points were much exceeded; in the same time these samples were exposed to an action of the solution the composition of which is similar to that of pore-liquid of concrete and additionally contaminated with chloride ions (pH = 9.14). The composition said was as follows: 0.015 M $\text{NaHCO}_3 + 0.005 \text{ M Na}_2\text{CO}_3 + 0.001 \text{ M NaCl}$. Corrosion rate for the steel has decreased by 2–3 orders of magnitude when covered with protective coating even though this latest became unseal at load exceeding 88.5 MPa.

References


SYMPOSIUM SESSION III
EVALUATION OF SAMPLE PREPARATION PROCEDURES FOR MICRO-MECHANICAL TESTING OF TRABECULAR BONE

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1. Introduction
To improve the knowledge about the mechanical properties of trabecular bone it is necessary to assess the properties at micrometer scale. Testing of isolated human trabeculae is highly influenced by precision of the sample preparation. Article deals with description of the pre-testing procedure for the uniaxial testing, nanoindentation and finite element modelling of single trabeculae. Undesirable mechanical, biological and chemical influences have to be excluded during this procedure.

2. Tissue harvesting
The samples were cut out from caput femoris using diamond blade saw (Isomet 2100, Buehler GmbH.). First, the samples were degreased. The cleaning process was performed using ultrasonic bath with 1% solution of Alconox (Alconox, Inc.) anionic detergent. Process of delipidation consists of 15min phase of ultrasonic cleaning at a temperature not exceeding 40°C and rinsing with distilled water. After the removal of all bone marrow the samples were immersed in an ultrasonic bath with distilled water for 5min. The sample was then dried at room temperature. In purified bone structure suitable long straight trabeculae were identified and carefully extracted using a sharp-tip scalpel.

3. Nanoindentation pre-procedure
Nanoindentation is a variety of indentation hardness tests applied to small volumes. During the test, an indenter is pressed down into the specimen surface while load and penetration depth are measured [1]. In our tests, the depth of the indent was less than 1μm therefore it was necessary to reduce the surface roughness of the samples to minimal possible value. The trabeculae were fixed in longitudinal and transversal positions in low shrinkage epoxy resin (EpoxyCure, Buhler GmbH.). Diamond grinding discs with grain size 35 and 15μm followed by monocrystalline diamond suspension with grain size 9, 3 and 1μm were used for grinding procedure regarding to the empirical grinding rule [2] with optimization described in detail in [3]. For the final polishing aluminium-oxide Al2O3 suspension with grain size 0.05μm on a soft cloth was used.

Fig. 1: 3D surface structure obtained by confocal microscope

Prior the mechanical testing the surface roughness of the sample was measured using a confocal laser scanning microscope (Lext OLS3000, Olympus Inc., Japan). Results of the scanning are represented by a 2D matrix of heights. The average roughness $R_a$ (average distance from the profile to mean line) of the finished surface was 25nm.

4. Uniaxial test specimens
The specimens were prepared for displacement-controlled tension and fatigue tests in a custom-based uniaxial loading device. This device was specially designed for testing of microscopic specimens [4]. The specimen deformation was captured optically using a high-
resolution CCD camera (CCD-1300F, VDS Vosskuhler GmbH, Germany).

The ends of the trabeculae were dipped in a two-component glue (UHUplus Schnellfest 2-K-Epoxidharzkleber, UHU GmbH & Co. KG, Baden) and stored for 48h at room temperature. The drops of glue at the trabecula ends were used for manipulation with the sample using a pair of tweezers. The manipulation droplets of glue were used to attach the sample to the end-plates of the testing device. Fast-setting glue (Loctite Super Attak Ultra Plastik, Henkel Ireland Ltd.) was used for this purpose and the glue was allowed to set for 2 hours prior the experiment at room temperature.

5. FE model development

Finite element model was developed for simulation of the tension tests. Shape of the trabecula was obtained from two orthogonal projections using CCD camera. The geometry of the trabecula was approximated by elliptical cross-sections. The surface was defined by ellipse and orthogonal splines were drawn through the keypoints.

The volume was discretized by 10-node tetrahedral elements with quadratic shape functions. The discretized model is depicted in Fig. 2. Material properties required for the simulation were obtained from nanoindentation tests. The loading was controlled by displacement. Obtained relation between applied displacement and reaction force was compared with results of performed tension tests.

6. Experimental Results

Pre-testing procedure for the uniaxial testing, nanoindentation and finite element modelling of single trabeculae was described in this work. Presented procedures (precise preparation and fixation of micro scale biological specimens) allowed carrying out a unique set of micromechanical material tests of human trabecular bone. It enables to compare the behaviour of single trabecula under physical uniaxial loading with FE simulation of the test.

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References

1. Introduction

Because of the need to save energy and resources, light weight materials are becoming more and more important. In this work, rotationally symmetric, aluminum-sheathed magnesium rods [1], produced by hydrostatic coextrusion, are investigated. The focus in this paper is on the determination of fracture mechanical properties of the newly developed AlMgSi1/AZ31 compounds (outer diameter 20 mm, core diameter 15 mm).

2. Linear elastic fracture mechanics of interface cracks

Fig. 1: Plane interface crack model

For the displacement discontinuities in \( r \) and \( \varphi \) direction at the near field of interface crack tips (Fig. 1), the following equation can be written in complex notation [2]

\[
\Delta u_\varphi(r) + i \Delta u_r(r) = \frac{8K}{E' (1+2\eta) \cosh(\pi\eta)} \left( \frac{r}{l_R} \right)^\eta.
\]  

(1)

The reference length \( l_R \) in Eq. (1) is arbitrary. The components of the displacement discontinuities \( \Delta u_j(r) \) at the crack flanks are given by

\[
\Delta u_j(r) = u_j(r, \varphi = -\pi) - u_j(r, \varphi = \pi) \quad j = r, \varphi.
\]  

(2)

Furthermore, the complex stress intensity factor \( K \) is defined by

\[
K = K_1 + iK_2 \quad |K| = \sqrt{K_1^2 + K_2^2}.
\]  

(3)

At interface cracks, mode I and mode II conditions always occur together and the indices are changed from I to 1 and from II to 2. Another input value of Eq. (1) is the mixed Young’s modulus \( E' \) in the plain strain state (\( E_{1/2} \) - Young’s moduli, \( v_{1/2} \) - Poisson’s ratios)

\[
E' = \frac{2E_1E_2}{E_1 + E_2}, \quad E'_{1/2} = \frac{E_{1/2}}{1-v_{1/2}^2}.
\]  

(4)

The bimaterial constant \( \eta \) is determined by

\[
\eta = \frac{1}{2\pi} \ln \frac{\mu_2 \kappa_1 + \mu_1}{\mu_1 \kappa_1 + \mu_2},
\]  

(5)

with the elastic constants \( \kappa_{1/2} \) and \( \mu_{1/2} \) (in the plain strain state)

\[
\kappa_{1/2} = 3 - 4v_{1/2}, \quad \mu_{1/2} = E_{1/2} / 2[1 + v_{1/2}].
\]  

(6)

Finally, for fracture mechanical analysis, the absolute value of the stress intensity factor \( |K| \) can be calculated by using Eq.’s (1) to (6)

\[
|K| = \frac{\sqrt{2\pi E' \cosh(\pi\eta)}}{8\sqrt r} \sqrt{\left( \Delta u_{\varphi}^2(r) + \Delta u_r^2(r) \right)(1 + 4\eta^2)}
\]  

(7)

and the energy release rate \( G \) (following [2]) is

\[
G = \frac{\pi E'}{32r} \left( \Delta u_{\varphi}^2(r) + \Delta u_r^2(r) \right)(1 + 4\eta^2).
\]  

(8)

\( |K| \) and \( G \) are independent of the reference length \( l_R \). If there are no differences between the elastic properties of the single materials, then \( \eta = 0 \) and the equations result in those, which are valid for the homogeneous case [3].

3. Experimental-numerical method

3.1 Procedure

The procedure for the determination of fracture mechanical values is presented in Fig. 2. The respective output values of the experiments and numerical simulations are written in the elliptic fields.
3.2 Experiments

For crack generation, notched specimens were clamped on the one side and loaded quasistatically by a punch on the other side. The results are crack lengths \( L_{\text{crack}} \) between 3.5 and 5 mm. The special specimen geometry is given in Fig. (3) (thickness \( t = 7 \) and 5 mm).

Fig. 3: Load case (upper) / part of the experimental setup of the crack propagation tests (lower)

Furthermore, the load case and the experimental realization of the crack propagation tests are demonstrated in Fig. (3). The load application angle \( \alpha \) depends on the specimen crack length \( L_{\text{crack}} \). This angle is determined by a FE analysis (paragraph 3.3) using the criterion of a mode I stress state in the homogeneous case. The specimens are loaded (quasistatically) by a punch and supported by two movable bearings at the Points A and B. After reaching the critical (maximum) force \( F_c \), brittle crack propagation occurs, which is associated with an abrupt force decrease.

3.3 Numerical simulations

The numerical simulations were done with ANSYS 12.1, using a fine 2d mesh (plain strain state) and quarter-point elements at the area around crack tip [3]. The input values for the simulations of the crack propagation experiments are the critical forces \( F_c \). In Fig. (4), an example of the \( \sigma_{\phi} \)-field is given. The output values for the fracture mechanical calculations are \( \Delta u_t(r) \) and \( \Delta u_{\phi}(r) \) at the crack flanks.

Fig. 4: Stress field \( \sigma_{\phi} \) at the crack tip region

4. Fracture mechanical results

Critical fracture mechanical values are finally calculated by means of the FEA results, Eq.’s (7) and (8) as well as the limiting values

\[
|K| = \lim_{r \to 0} |K|_{KEA}(r) \quad G_c = \lim_{r \to 0} G_{KEA}(r).
\]

Tab. 1: Fracture mechanical results of the test series

| \( |K|_c \) [MPa m^{1/2}] | \( G_c \) [N/m] |
|-----------------|----------------|
| 2.3..3.3        | 88..178        |

In case of the used materials (\( E_{\text{Mg}} = 45 \) GPa, \( E_{\text{Al}} = 70 \) GPa, \( \nu_{\text{Mg}} = 0.35 \), \( \nu_{\text{Al}} = 0.33 \)), the influence of the bimaterial constant \( \eta = 0.013 \) is low, so that it can be reasoned that \( |K|_c \approx K_{Ic} \). The fracture mechanical behaviour of the interface is independent of the crack length and can be classified as brittle (Tab. 1).

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References

PROCESS TO INSERT NITINOL STENTS INTO THE PERIPHERAL VENOUS CATHETER

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1. Introduction

In the European Union the most often reason of deaths is the disease of the cardiovascular system. The so called stent is a biocompatible metal mesh which is inserted in the narrowed section of the artery to dilate and prop up its wall; hereby it ensures continuous flow of blood, inhibiting for example the formation of stroke [1]. In pursuance of our research stents made of shape memory Ni-Ti alloy (nitinol) were investigated which can be exceedingly applied in peripheral arteries exploiting the special features of nitinol.

2. Experimental

These stents were developed, cut out, etched and electro-polished by our research group and implanted into the carotid artery of rats by a physician group to examine how they act in biological organism. The stent is put in the artery by the help of a peripheral venous catheter. Before the implantation, the stent with 1 mm outside diameter must be inserted in the thin tube of the catheter, which inner diameter is 0.3 mm. The physicians push the stent out of this tube to the rat artery by means of a needle where it dilates to its initial size. The finishing temperature of austenite transformation of nitinol must be adjusted under body temperature to ensure the entire dilatation; furthermore the sufficient mechanical properties of the stent at body temperature must be satisfying to the sufficient working of the stent.

The first emerging problem while the stent was inserted in the catheter was the tiny size of stent. This problem manifested that the developing process must be the simplest as far as possible considering nippers must be used to the simple grip of the stent paying attention to the intact of struts.

The next problem stemmed from the insurance of needed under -10°C temperature for reversible strain and from the convenient deformation of stent. The solution was searched in special features of nitinol, because of austenite-martensite transformation of nitinol due to thermal effect evolving so called single way shape memory feature and superplastic behaviour of nitinol in martensite phase was exploited. The kernel of single way shape memory phenomena is that if nitinol is cooled under finishing temperature of martensite transformation, where it can be deformed superplastic thanks to less symmetry of martensite phase, than warming it back it is taken on its initial shape transforming into more symmetrical austenite phase (Fig 1) [2,3].

Fig.1: The process of lattice structure

There are different ways to the transformation into martensite phase but the reconformation into austenite phase can happen only one way; accordingly in deformed material from phase transformation and from deformation stemmed stress is dissolved due to a warming effect while retransforming into austenite phase it is able to recover its initial structure [2,3].

During the deformation the full martensite structure of stent must be ensured by constant under -10°C temperature. For this a chemically
harmless (in the aspect of stent and human) vehicle was needed which is convenient to carry the insert in it out and its melting point is low enough. Ethanol proved eligible (melting point is: -114°C), approximately 500 ml of it was cooled to -25°C by fluid nitrogen then poured into a 12×20×10 (mm) sized and with Styrofoam thermal isolated pot made of PP. The warming intensity of this system was 1°C/min according to our observation: the temperature of ethanol was continuously checked by a thermometer (Fig. 2).

![Fig. 2: Ethanol bath](image)

The sufficient deformation of stent plays an important role by the insert. According to our earlier experiments it was determined that an equable, reversible and almost 70 percentage radial deformation is needed which ensures the intact of struts and cylindrical shape of stent by the process. The deformations of the struts have a limit because of the properties of nitinol.

The meshed structure of stent results high degree of radial deformation. The above mentioned deformation can be successfully performed by a rolling process. During this process the diameter of the stent reducing while the length of the stent is increasing. In the course of this process, the rolling terms of the stent must be met; hence to compensate the reduced friction conditions because of the ethanol, an abrasive paper (P600) was fit on the surface of the rolling tool.

After the deformation process, the stent can be placed into the venipuncture. The difficulty of the process arise from the location of the tube, which located 10 mm farther from the proximal end of the venipuncture and during the pushing movement the stent can flare out. To eliminate this problem, a cone-shaped tunnel was produced, fixed and centralised to the entrance of the venipuncture. The advantage of the cone-shape is that the stent can be easily placed even in the ethanol bath and this narrowing cross-section can help further reduce in the diameter of stent. The stent was pushed through in this cone-shaped tunnel with a metal guide catheter (its OD is less, than 0.3 mm), into the venipuncture distal tube (Fig 3). The insertion was smooth and quick.

![Fig. 3: Process of insertion](image)

3. Experimental Results

Our aim was to work out such a reproducible and reliable method, through which the stents, manufactured by our research group can be placed into the venipuncture without damage. The success of this step is important and decisive in the aspect of the whole research. The problem was investigated, resolved and the method is worked out. Using this method, altogether twenty stents were put into the venipuncture.

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References


DAMAGE ANALYSIS OF AUTOMOTIVE TURBOCHARGERS

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1. Introduction

This paper presents the results of one part of research on Innovation Project, with emphasis on often fails to work of turbochargers which is used for passenger vehicles, [1].

Turbochargers are widely used in trucks and diesel engines. There are also some gasoline fueled cars and other special purposes vehicles that use turbochargers. A small turbocharger, Fig. 1, consists basically of a compressor and a turbine coupled on a common shaft, [2].

Fig.1.: Turbocharger

The most important factor in the design of an automotive turbocharger is the initial cost. Engineers aim to undercut the cost of producing larger engines capable of providing the same power. Even though truck engines operate at modest break-mean effective-pressure (~14 bar) and hence at lower boost pressure (up to 2.5:1), they work at much higher exhaust temperature [3]. Because of truck engines heavy operations, they demand good acceleration and high torque over a wide speed range. They also require a high level of reliability and efficiency.

There are several ways to reduce cost of turbocharger. Should be kept simple design and selected materials need to satisfied the working conditions. One should bear in mind that using Inconel Alloys, Iron, Bronze, etc.

The compressor impeller, Fig 2, in most automotive type turbochargers is made of aluminum. Aluminum is also used for the compressor casing, unless the compressor impeller is made from other material than aluminum. On the other hand, the turbine rotor should withstand a much higher operating temperatures that could be as high as 1000 K, or more. Therefore, the most convenient material to use for that purpose is 713C Inconel, a high nickel alloy.

The turbine rotor casing should also withstand high temperatures, but not resist as high pressure as the turbine. There are three different types of materials used for the turbine rotor casing depending on their operating temperatures. S.G. iron, spheroidal graphite, is used for operating temperatures up to 975K, high-silicon S.G. iron is for temperatures up to 1000K, and high nickel cast iron for temperatures above 1000K. The shaft is usually made of high-carbon steel to allow induction hardening of journals, [2].

Fig. 2. Compressor impeller

Most of turbochargers for commercial vehicles incorporate simple journal bearings. They use the engine lubricating oil system for their bearings to assure low cost and simplicity of maintenance, instead of having a separate system. Ball bearings are not used for most commercial engine applications because of their short life and difficult access for replacement. Special high performance engines in automotive racing applications, can afford the added expense of ball bearings.

2. Damage Analysis

When there is damage on turbocharger the most important is to find the cause of failure, because that is the only way to solve a problem with turbocharger. Researching in project, [1], and bearing in mind researching manufactures of turbocharger, Garrett, Holset, Mitsubishi, Schmitzer, Borg Warner, Hella, etc. damage of turbochargers can be divided into four major groups.

2.1 Oil Contamination

Fine particle contamination, may not be noticed in oil visually, but causes polishing of the bearing surface and tell tale rounding of the outer edges.
Often the compressor end bearing may be worn to a taper on the outside diameter.

**Fig. 3.** Oil contamination, small part a), large part b).

Large particle contamination, oil borne large particles, may cause impact damage and deep scoring as shown, Fig. 3. b). The bearing bore may also be scored, usually to a lesser extent, The shaft and centre housing are usually damaged slightly less, being harder materials. The light scoring right was caused by large oil borne contaminations.

### 2.2 Lack of Lubrication

Marginal lubrication where the oil supply to the turbo is reduced, for instance when gasket materials partially block an oilway or inlet flange. Characterized by extreme discoloring of shaft journals, Fig. 4.

**Fig. 4.** Lack of Lubrication

Chemical contamination causes heavy wear of bearing/shaft and excessive temperature. The visual indications are very much the same as for Marginal lubrications. Total Lack of Lubrications for similar causes, will show similar damage, but more extreme. Damage happens very rapidly.

### 2.3 Exceptional Operating Conditions

Typical damage is high temperature at the bearing journals, on severe examples, the oil burns and “cokes” the shaft. Often the back face of the turbine wheel is slightly concave, usually accompanied by an “orange peel” – very clear signs of overspeeding and overboosting.

**Fig. 5.** Overspeeding/Overboosting

Overspeeding can also cause the loss of a portion of turbine blades. The damage may look similar to foreign object damage, but is often accompanied by cracking at the exducer blade root and in extreme cases, the wheel can burst due to overspeeding. Minute stress crack appear as the wheel is “stretched” beyond its designed limits and these gradually increase during overspeeding cycles followed by a final rapid failure.

### 2.4 Foreign Object Damage

Hard foreign object–this damage was caused by foreign object entering the compressor. The object may bounce around in the compressor inlet causing the type damage, seen Fig. 6a. Salt or sand causes severe erosion and corrosion, eventually leading to blade failures.

**Fig. 6.** Foreign object damage

Soft foreign object such as clothes or even paper wipes can cause damage, Fig. 6b. Typically, the blades bend backwards and in extreme cases sections of the blade may break away due to metal fatigue.

Hard foreign object entering the turbine will damage inducer blades as shown, Fig. 6c. Even small objects such as rust scale, from manifold, can cause considerable damage to such high speed components.

### 3. Conclusion

This paper is the basis of the turbocharger, as well as four basic types of damage. What is the analysis of these defects and their causes, a conclusion which can be done is to fail to work turbocharger for passenger vehicles are due to problems outside of the turbocharger.

In fact the most turbo failures are caused by problems outside of turbocharger.

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PROJECT AND CONSTRUCTION OF A PAIR OF CLAMPS TO PERFORM TESTS ON SYNTHETIC FIBER ROPES

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1. Introduction
To guarantee the total reliability of an anchorage system of a petroleum platform, it is essential to test intensely the ropes used on this application, ensuring a perfect idea about their properties. However, testing synthetic fiber ropes is not an easy task. To do that, it is important that these tests could be applicable for most different architecture, size or material as possible, reproducing the real studied rope characteristics.

POLICAB (Stress Analysis Laboratory) has been studying this subject for a long time, trying to create a device that could perform reliable tests. Therefore, the present paper will talk about the last one developed in the laboratory, the Sandwich Clamps for 10 ton load tests performance.

2. Previous Researches
In 2006, considering the lack of devices created to perform tests on synthetic fiber ropes, Pfarrius [1] started to project a different apparatus to solve this great problem. The idea was to hold the ends of a specimen with a kind of pair of clamps, using some glue for it. Each clamp would be constituted by two parallel steel plates made of AISI 4340 (picture), which would be screwed one to another. So, this device was designed for 2.5 ton loads and constructed at POLICAB - FURG (Federal University of Rio Grande), receiving the name of Sandwich Clamps.

After that, some tests were performed using the clamps. They were considered satisfactory, and then Pfarrius [3] exposed its solution at the 6th YSESM, on Serbia. The pair of clamps still exists and works well on POLICAB.

3. Sandwich Clamps for 10 ton Loads
In view of the necessity of testing larger ropes, it was decided to design a new pair of Sandwich Clamps in 2010, with a capability to support 10 ton loads.

In the first place, the necessary area of contact between the glue and the aluminum plates to support the shear load was defined. It was calculated on 211 cm², and distributed just like it is shown in the picture (Fig. 2). The geometry was chosen to better fit the filaments of the specimen end.

After that, the steel plates and the basis of the clamps were designed, considering for it tension loads of 10 ton.

In the end, the device was constructed at FURG mechanical workshop. It is possible to take a look at the new Sandwich Clamps and its appropriate attachments for the testing machine in figure 3.
4. Pre-Test Procedure

Preparing the specimen to test with the Sandwich Clamps requires a lot of attention, and some steps need to be followed. So, it was used the procedure already developed by Pfarrius [1] to do it. The steps:
1. Choose a specimen of 20 cm length;
2. Paste each end rope between two aluminum plates;
3. Apply a compression load on the plates while the glue sets;
4. Position each end in each clamp;
5. Screw the clamps.

After this, it is finally possible to place the clamps on machine and perform the test.

5. Experimental Results

Some tests were performed to verify the effectiveness of the Sandwich Clamps constructed. They were realized between December 2010 and March 2011 at POLICAB.

However, the results obtained were not good. A slippage problem was verified on every test. The adherence between the glue and the aluminum plates wasn’t enough to support the rope breaking load.

Three different glues were tried out: Araldite® Professional 24 hours, Tigre® Plastic Adhesive for PVC, Poxipol® Transparent - 10 min. Among these, the best results were obtained using the third one. But it was still not enough. The highest reached load was 5.72 ton, when some filaments started to slip from the plates.

At the end, the aluminum plates were replaced by pressed wood (Eucatex®) ones. Even this way, the result wasn’t good, reaching a 6 ton load.

The tests were performed into an Instron 8800 Servohydraulic Fatigue Testing Machine up to 100 kN Capacity, with a Dynacell® load cell for the same 100kN. The curve of figure 5 shows the result of one of the tests performed with aluminum plates and Poxipol®.

6. Conclusion

After all of those tests, it is concluded that the Sandwich Clamps did not work the way it was expected because a slippage between glue and aluminum plates happened. Different materials would be tested from now on. This way, the studies will continue, looking for a pair glue/plate with enough adherence to make the device work well.

References

1. Introduction

The aim of the research was to identify the loads on multicaterpillar track chassis and calculate the strength of elements of chassis. The measurements were taken on open cast mine machine: mobile transfer conveyor, shown on figure 1. The biggest problem in such kind of machines is to properly diagnosis the state of machine and remove from usage before any dangerous, for people and machine, accident would have take place [1,2].

2. Boundary conditions

The chassis of open cast machines like mobile transfer conveyor consists of up to 12 caterpillar track connected into sets of 2 or 4, of which some are steered. The scheme of chassis that is the aim of research is shown in figure 2.

The typical scheme of loads are shown on figure 4. The boundary conditions for the drawbar are connected with the most adverse work conditions - steering the steered caterpillar track set. The Loads on the caterpillar track are more complicated than the loads on drawbar. The boundary conditions are related to the ride of the entire machine. During the turning the forces try to break the caterpillar track [5, 6]. During the forward ride the most endanger elements are axis of caterpillar track. Nowadays norm and engineering practice shows that factor of the friction is held at the value of 0.6. Interaction ground and caterpillar is also included in loads on caterpillar.

3. Numerical measurements

Numerical analysis were done using the Finite Element Methods. Calculations were done for 5 different schemes of loads on caterpillar track and for 2 different schemes of load on drivebar.

During the calculations nonlinear material model was used. Some results of the calculations
are shown in figure 4 and 5. Indication of the most endangered places were based on results of analysis.

![Fig. 5: Results of numerical analysis](image)

4. Strain gauges measurements

Strain gauges measurements were performed in 9 measuring points: 6 points on track's supports, 2 on caterpillar’s track support and one on drivebar [3]. Scheme of measurements points is shown in figure 6. The measurements were taken by multichannel registrar during the riding the conveyor. The measurements were taken for many settings of body and chassis with different angle of steering set.

![Fig. 6: Scheme of strain gauges measurements](image)

5. Results of strain gauges measurements

After examination the results from measurements there were shown some figures, that shows tubing of caterpillar sets, increasing and decreasing steering force and track force on caterpillar during riding and turning. The results of measuring the steering force is shown in figure 7. Those measurements are from trusses and were done during the ride forward, backward and turning right from angle 0° until 13°.

![Fig. 7: Results of multichannel registration](image)

6. Summary

Nowadays there is lack of guidelines to take the appropriate boundary conditions and exploitation loads. It is necessary to take more research on open cast mining machines to more properly define the loads on multicaterpillar track chassis.

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SYMPOSIUM SESSION IV
EXPERIMENTAL DETERMINATION OF OPTIMAL DOSAGE ACTIVATOR FOR POZZOLANIC BINDER FORMULATION

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1. Introduction

The use of mixes stabilized with pozzolanas has been internationally established since 1987, during the International Roads Congress held in Brussels, and it is ascertained that pavements with stabilized layers were already widespread in the entire world [1].

In Romania, although the thematic scientific preoccupations have been synchronic to the international ones and have been finalized with the elaboration of instructions and norms to be applied, there has been no resort to current application, due to bureaucracy inertia as well as to professional biases of road constructors, stimulated by some inadvertences of the normative stipulations and by the severity of the imposed technical condition for this technology.

The Romanian regulation CD 127-2002 [2] recommend the use of some aggregate-pozzolana-activator mixes in the following compositional formulae reported to the total weight in dry state:

- aggregate + 20, 25, 30 % blast furnace slag
- aggregate + 10, 20, 30 % fly ash
- aggregate + 6, 8, 10 % pozzolana.

The activator dosage (according to the 74th article of CD 127-2002 [2]) is indifferent unto the kind of pozzolana used:

- 2% lime or cement, if the mix is grouting in fixed stations;
- 3% lime or cement, if the mix is made in situ.

The prescription of dosages by 2% or 3% activator-lime or cement regardless of diversity and quantity of activated pozzolana is scientifically unsubstantiated, because the quantities of activator must strictly correlate with the chemistry of activated pozzolana, so that the dosage of activator must needs related to quantities (dosage) and the type of pozzolana, and not to the performance technology: grouting in fixed stations or made in situ.

2. Test data

Within the laboratory researches it was followed by experimental way the appraisal of the optimum dosage of activator – lime, optimum dosage being the percentage of line related to the pozzolanic mass where for the compressive strength on stiff pastes made of pozzolana-lime couple and water has maximum value.

For practical reasons, during lab researches for establishing the compositional formulae of pozzolanic binders the "step-by-step" strategy as adopted consisting in successive tests of quality and quantity combinations between various materials (pozzolana, activator agent, water) followed by testing of physical and mechanical parameters for validating the optimum variant.

The following compositional variants were tested:

- V1 - Portland cement II-AS 32.5R (as a dummy specimen)
- V2 - fly-ash + lime, various proportions
- V3 - coarse blast furnace slag, commercial sort 0-8 + lime
- V4 - milled volcanic tuff + lime
- V5 - milled granulated slag + lime
- V6 - milled granulated slag + Na(OH)

The pozzolanic material - fly ash, blast furnace slag and pozzolana - were grind at a grinding smoothness characterised by the whole passing through the sieve by 90 μm.

The pozzolana-activator agent mixes in strictly determined proportions were homogenized in the ball mill for 30 minutes. Water was added in order to obtain equal consistency pastes, then cast into metallic molds of 2 x 2 x 2 cm and compacted on shock table, then maintained in the following conditions:

- 48 hours in molds covered with PVC folium at 20…22°C;
- 48 hours after removal of molds, in water at 22…24°C;
- 66 hours in water at 60°C (gradually heated from 24°C in thermostatic bath);
- cooling in atmosphere for 6 hours up to 22°C
Fig. 1: Binder CvV fly ash + lime CvV30. Optimum binder formulation: fly ash + 30% lime of fly-ash mass

Fig. 2: Binder TvmV grounded volcanic tuff + lime TvmV50. Optimum binder formulation: tuff + 50% lime of tuff mass

Fig. 3: Binder ZmV granulated ground slag + lime ZmV10. Optimum binder formulation: slag + 10% lime of slag mass

After seven days of maturation in the mentioned above regime the specimens were tested for compressive strength ($R_c$). The activator agent content corresponding to the maximum compressive strength designates the optimum composition of the investigated pozzolanic binder.

The validated variants are shown in figures 1, 2, 3.

3. Conclusion

The complex thermal analyses (DTA + DTG) make evident the evolution of hydration and hardening processes by formation of CSH phases due to calcium and silica from binder constituents, and of AFM respectively, due to calcium and alumina. During researches, the activator agent with optimal dosage was entirely consumed.

The binders previously established are classified in resistance classes stated in SR EN 196-1 and SR ENV 13282/2002, as follows:

V2 – fly-ash + 20% lime
V5 – milled slag + 10% lime
V4 – milled tuff + 50% lime

The principle of this method was, also, extended to other origin sources of the pozzolanic materials and the experimental results confirming the applicability of the researches undertaken in this study.

The testing of various pozzolana-activator formulas showed a positive correlation between the nature of pozzolana and the proportion (dosage) of activator shows that the stipulation, in accordance with the CD 127-2002, for use a general dosage of 2 or 3%, depending on the technology of preparation of mixtures, is obsolete.

References


DETERMINATION OF THE PROBABILITY OF FAILURE OF TURBOGENERATOR ROTORS BASED ON LCF EXPERIMENTATION

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1. Introduction

Turbogenerator rotors are a typical example of large components experiencing low cycle fatigue (LCF) [1]. Every machine switch-on and switch off corresponds to a LCF cycle, for a total amount of 10,000-15,000 cycles in the whole machine life. In [1], an experimental study was presented, concerning the characterization of two materials for rotor and coil retaining ring (CRR) manufacturing.

The design task of rotors and CRRs involves serious safety issues: incidents caused by unexpected failures may lead to rotor explosion with catastrophic effects. For this reason, the structural analysis must be integrated by the estimation of the probability of failure in the machine life stages, to be fulfilled by a suitable probabilistic method. However, there are very few papers in literature (e.g. [2-3]), tackling this issue. The object of this paper is to show a suitable methodology to quantify the safety of a rotor, starting by the knowledge of LCF experimental data and of the nominal loads on the shrink-fit coupling with the CRR.

2. Methodology and results

The fatigue curves determined in [1] where processed for the computation of the fatigue strength ($\sigma'$) and ductility ($\varepsilon'$) coefficients and of the fatigue exponents (b, c). In [4] it was shown that a statistical model [5] can be used for the determination of the standard deviations of the aforementioned material parameters. In Fig. 1 the fatigue curve of the rotor material is sketched together with is lower and upper bounds, to account for the worst scenario of twice the standard deviation. Afterwards, a log-normal distribution was presumed for $\sigma'$ and $\varepsilon'$ and a normal one for b and c [6-7]. A similar procedure was adopted for the determination of the normal distributions of the static and plastic coefficients of plasticity (K, K') and related hardening exponents (n, n'). The mean values (µ) and the standard deviations (σ) of the eight random variables are summarized in Table 1.

The determination of the probability of failure can be performed by running a Monte Carlo simulation, however this method is computationally expensive for problems with a high reliability. Alternative approximated methods, such as AFOSM or AMV [6-7] are based on a polynomial approximation of the functional relationship $h$ between the inputs (listed in Table 1) and the output variable, the logarithm of the expected life $Lg (N) = U_0$.

$$Lg (N) = U_0 = h (\alpha) + \sum_{i=1}^{8} \frac{\partial h}{\partial U_i} (U_i - a_i) + \sum_{i=1}^{8} \frac{\partial^2 h}{\partial U_i^2} (U_i - a_i)^2$$
Eq. 1

The vector $\alpha = (a_1, \ldots, a_8)$ indicates the expanding point: its components are initially selected as the mean values of the eight variables.
The derivative terms were determined by fitting procedure [6]. The following step consisted in rearranging Eq. 1 in the form of a failure function g, as in Eq. 2, where \( N_p \) indicates a generic life duration, for which the failure probability is estimated.

\[
g(U_1, \ldots, U_8) = \log(N) - \log(N_p) = 0 \quad \text{Eq. 2}
\]

After introducing the reduced variables \((u_1, \ldots, u_8)\), a safety index \( \beta \) was computed by solving the constrained problem (Eqs. 3-4).

\[
u_i = \frac{U_i - \mu_{U_i}}{\sigma_{U_i}} \quad \text{Eq. 3}
\]

\[
\begin{align}
g(u^*) &= 0 \\
\beta &= \min \left\{ \sum_{i=1}^{8} u_i^2 \right\}
\end{align} \quad \text{Eq. 4}
\]

Finally, the probability of failure \( p_f \) was computed by applying Eq. 5, where \( \phi \) is the standard normal distribution function.

\[
p_f = \phi(-\beta) \quad \text{Eq. 5}
\]

The described procedure was then iterated until \( \beta \) convergence: the vector \((u^*)\) was considered as the expanding point at the following iteration. The machine safety was finally quantified, by computing the safety index and the probability of failure at different stages of the machine life (Fig. 2).

![Machine life vs. Probability of failure](image)

**Fig. 2:** Safety evaluations: determination of the safety index and of the probability of failure

### 3. Discussion and conclusive remarks

The following observations can be made with reference to the calculation procedure and the determined results.

- Considering \( K, n, K', n' \) as random variables was very important, to account for the scatter of static and cyclic curves and for the random local strain history, even under a deterministic nominal load.
- The proposed numerical procedure proved to work well: both first-order and second-order models were developed and both led to convergence after few iterations, with negligible computational times.
- The Monte Carlo method proved to be inefficient to calculate the probability of failure in the machine life range, but was used anyway to validate the results outside this range with a very good agreement.
- The determined values for \( p_f \) and \( \beta \) at the end of machine life, respectively \( 5 \cdot 10^{-6} \) and 4.4 are both acceptable with reference to the safety requirements of several structures under fatigue, even in the nuclear field [8].

### References


BUCKET WHEEL EXCAVATOR MODAL MODELS DETERMINATION IN DIFFERENT OPERATIONAL CONDITIONS

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1. Introduction

Excavation process in open cast mines requires heavy machines which work in very hard conditions. The machines are strongly exposed to dynamic loads during operation [9]. The problem of identification of modal modes is additionally complicated while the operational conditions changes. Type of excavated material, configuration of the machine, balancing and many other factors can shift frequencies or even influence on mode shape.

2. Object of investigation

Bucket wheel excavator of the type KWK 1200M was the object of investigation. The theoretical capacity equals 3750m³/h. The wheel with 10 bucket excavates with nominal velocity 2,29m/s, which gives dumping with frequency of 0,87Hz. Total mass of the machine (fig. 1.) equals around 1823Mg.

![Fig. 1: Bucket wheel excavator KWK1200M](image)

3. Measurements run

The main problem in performing experiment on such a big object like BWE is proper selection of measurement points. The incorrect placement of acceleration sensors may lead to the spacial aliasing [7],[8]. This means that some of mode shapes can be deformed or even can left unidentified.

In case of the bucket wheel excavator KWK1200M, accelerometers were placed in 14 points. Direction of measurement were also chosen in such a way to allow good identification of modal modes. The placement of accelerometers and measurement directions are shown in the figure 2.

![Fig. 2: Measurement points](image)

Data were acquired during excavation and ride of the machine. In the first case, material was excavated in clockwise direction and then in counter clockwise direction. The ride also consist of two sub cases. The first one was short ride with stop and time to attenuate vibrations [6]. The second one was long ride and additionally the load carrying structure was rotated to the right (perpendicular to the ride direction).

4. Operational modal analysis

To estimate modal parameters from output only data the operational modal analysis algorithms were applied [10]. Modal identification was done for each sub case of excavation and ride separately. As a result, four modal models were estimated. Modes looks to be the same, but closer analysis allows to distinguish differences and derive its causes.

For proper comparing, the MAC factor was used. With its use only frequencies of highly correlated mode shapes were compared. However,
in some cases low value of MAC factor indicated change of the mode shape itself.

When comparing sets of excavation it occurs that during excavation in counter clockwise direction, mode 4 (fig. 3) was not excited at all. This means, that changed horizontal excavation force, that can be treated like a support, change the occurrence of modes.

Fig. 3: Mode 4 - excavation

In ride cases, significant difference was observed in mode 2 (fig. 4.). The frequency changed from 0.966Hz to 0.925Hz. This change was caused by the position of load carrying structure. In the first case, it was positioned in the direction of the ride and in the second the position was perpendicular to the ride direction.

Fig 4: Mode 2 - the same for ride and excavation

The most important information about the structure derives from comparison excavation with ride modal models.

In mode 4th and 5th the change in frequency equals around 0.15Hz but the most important is level of MAC factor. Comparison of the 4th mode from ride and excavation gives very low value of MAC (37%). In case of the 5th mode the MAC equals 73%. When compare visually, it is easy to observe change in vibrations of counterweight. The load on the machine lowers the amplitude of counterweight vibration almost to zero.

5. Conclusion

Operational modal analysis allows to identify modal models of brown coal machinery in different operational conditions. Thanks to this approach, changes in object dynamics can be observed.

The excitation on BWE is always very close to the natural frequencies. Knowledge about changes in dynamic characteristics allows to avoid resonance which is quite common phenomenon in brown coal machinery operation.

Acknowledgements: Research co-financed by the European Union within the European Social Fund.

References

1. Abstract

The paper presents the evaluation of the pedestrian safety, during a collision with a road vehicle with the high bonnet leading edge. A Sport Utility Vehicle (SUV) was chosen to investigate the influence of vehicle front design on the pedestrian lower extremity injuries. The Finite Element Method (FEM) was utilized in order to reduce the costs and time needed to carry out a pedestrian-to-car front aggressiveness test. The virtual tests carried out by means of the numerical, certified Finite Element (FE) lower leg impactor were further contrasted with the results of the multibody (MB) simulation. While the FE impactor gives in-depth data about leg fractures and knee joint injuries, further MB simulations present the complete, post-impact pedestrian kinematics. Finally, the critical points in SUV design, in terms of pedestrian passive safety enhancement, were indicated.

2. Introduction

Pedestrian safety has become a key issue in the field of vehicle safety regulations in European Union, where 12-25% of seriously injured or killed people on roads account for pedestrians [1]. However there is still lack of data measuring the performance of the frontal protection systems, also known as bull bars (fig. 1), in the vehicle to pedestrian collision.

In this paper a novel approach has been presented – the combinations of the FE impactor subsystem test and MB simulation with the standing Hybrid III 50th percentile dummy.

3. Finite Element approach

Once the discrete model of the Sport Utility Vehicle (SUV) and frontal protection system were completed, the authors had to verify the parameters encompassed in the Regulation (EC) 78/2009 [2]. The explicit LS-DYNA code was used to verify the frontal protection system performance against the biomechanical limits. Fig. 2 depicts the visualization of the collision between the SUV, with the bull bar fitted, and the pedestrian’s virtual leg. The numerical impactor, which imitates the performance of the lower extremity, can be spotted in the figure.

Fig. 1: The frontal protection system fitted to Nissan Navara, close-up

Fig. 2: Discrete model of Nissan Navara and lower legform impactor

The FE simulation brought some crucial data concerning the lower leg injury risk [3]. It is especially important since a bumper of a SUV strikes usually pedestrian’s thigh. This leads to complicate ligament injuries and, what is worse, the reaction and friction force may cause the pedestrian to be dragged under the car chassis. However, it is not possible to investigate the kinematics of the pedestrian using only the impactor. Hence, the full dummy model was further used.
4. Multibody approach

Although the current regulations make the vehicle manufactures to test the vehicle fronts with the impactors, the examination might not mirror the actual kinematics of an impacted pedestrian. Therefore, the authors carried out a multibody simulation by means of MADYMO.

The vehicle front-end was modelled with simple ellipsoids and surfaces. The attention was paid to reflect the dimensions, not the material properties of the vehicle and bull bar. As the distinct from the FE, for the kinematic analysis evaluation there is no need to model detailed material characteristics. The geometry of the impacting body – i.e. the vehicle – is the key factor for ensuring the pedestrian safety post-impact trajectory.

5. Conclusion

The increasing number of SUVs has a serious implication on pedestrian passive safety. The carried out FE and MB simulations highlighted some drawbacks of the compulsory impactor subsystem tests for new, high bumper vehicles (SUVs). It has been proved that the kinematic of the lower leg differs from dummy model (fig. 3).

Fig. 3: a) lower leg imactor behavior (FE); b) dummy model kinematics (MB)

Hence, despite of meeting the biomechanical criteria obtained from the FE impactor test, the pedestrian actual post-impact kinematic cannot be accurately verified. Consequently, for vehicles with high bumpers and high bonnet leading edges, the test procedure proposed in current regulations may not fully asset the pedestrian safety (fig. 4).

Fig. 4: Differences in post-impact kinematics in FE (a)) and MB (b)) numerical models

The obtained and presented results suggest that for SUV-type vehicles the dummy test would be recommended. The great development of computation power and expansion of FEM and MB analyses enable to widen the possibilities and application field of numerical simulations. While FE impactor can in-depth check the injury risk of lower legform, the MB simulation validates the overall pedestrian kinematics which is directly related to the vehicle’s front geometry.

References

[1] European Commission, Move to improved pedestrian safety, IP/08/964, Brussels 2008
1. Introduction
The challenge was to calculate the tooth root strength of worm wheels [1]. This is important for a better prediction of cracks (Fig. 1). To use local stresses is a suitable way in comparison with rated stresses, especially in the case of complex shaped components.

Fig. 4: tooth with cracks at the tooth root notch

The local stresses can be identified due to FEA calculations [2]. To evaluate the local stresses, the FKM guideline [3] is an appropriate tool. Currently, the FKM guideline is only valid for steel and aluminium materials. Within a research project the usability of the FKM guideline for bronze materials could be shown. As shown in Fig. 2, a lot of experiments on specimens up to complete worm gears and finite element analyses were realized.

Fig. 5: adaption and validation of the FKM guideline

2. Calculation process
The calculation process followed the FKM guideline and is based on the determination of a degree of utilization. It was ascertained by comparison the bearable stress $\sigma_{AK}$ with the present stress. The bearable stress is evaluated on the basis of tensile strength, geometrical and material parameters. The parameters of the calculation process are shown in Fig. 3.

The central problem was to determine the factors which depend on material: the constants $a_G$ and $b_G$ to calculate the notch sensitivity $n_\sigma$ and the stress gradient $G_\sigma$ was predictable with parameters from the FEA.

\[
\sigma_\sigma = \frac{a_G}{b_G} \cdot \sigma_\sigma + \frac{b_G}{a_G} \cdot \sigma_\sigma \cdot n_\sigma
\]

In order to calculate $a_G$ and $b_G$, the notch factor $K_\sigma$ was identified for a special notch geometry by four point bending tests [5] on notched test samples.

3. Determining bronze parameters
To determine the notch sensitivity $n_\sigma$ (3.01) for $0.1 < G_\sigma < 1$, the two factors $a_G$ and $b_G$ for bronze material and the stress gradient $G_\sigma$ were needed. $G_\sigma$ was predictable with parameters from the FEA.

\[
n_\sigma = 1 + \sqrt{G_\sigma} \cdot \frac{R_\sigma}{10 \cdot n_\sigma \cdot \text{MPa}}
\]
specimens (Fig. 4). The form factor $K_t$ was determined with the help of the FKM Guideline according to [3].

These two factors permit the calculation of $n_\sigma$ out of tests according to equation (3.02).

$$K_t = \frac{K_{t1}}{n_\sigma} \tag{3.02}$$

With the $n_\sigma$, a determination of $a_G$ and $b_G$ was possible. These two parameters (Table 1) are valid for the material CuSn12Ni.

**Table 1:** parameters $a_G$ an $b_G$

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<td>$a_G$</td>
<td>0.05</td>
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<tr>
<td>$b_G$</td>
<td>1050</td>
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The consideration of the mean stress factor $M_\sigma$ depends on material and describes the dependency of the amplitude of fatigue strength of the component and the mean stress. To determine $M_\sigma$ (3.03), the material constants $a_M$ and $b_M$ were needed.

$$M_\sigma = a_M \cdot 0.001 \cdot \frac{R_m}{\text{MPa}} + b_M \tag{3.03}$$

These two constants were reversely specified with the help of the mean stress factor $K_{AK\sigma}$ (3.04). It’s possible to evaluate this factor out of the pulsating bending strength $\sigma_{AK}$ (out of own bending tests) and alternating bending strength $\sigma_{WK}$ [4], whereas for the bronze material CuSn12Ni, $\sigma_{AK}$=280 MPa and $\sigma_{WK}$=140 MPa. The factor to take account of residual stresses $K_{En}$ was according to the FKM Guideline set to 1.

$$K_{AK\sigma} = \frac{\sigma_{AK}}{\sigma_{WK} \cdot K_{En}} \tag{3.04}$$

$$K_{AK\sigma} = \frac{1 + M_\sigma / 3}{1 + M_\sigma} \tag{3.05}$$

$$\sigma_a = \frac{1}{2} (\sigma_{\text{max}} - \sigma_{\text{min}}) \tag{3.06}$$

With the value of $K_{AK\sigma}$ and the equation (3.05), $M_\sigma$ was developed in an iterative process. The mean stress $\sigma_m$ and stress amplitude $\sigma_a$ were taken from the four-point-bending-tests, whereas $\sigma_a$ was determined with equation (3.06).

With the known $M_\sigma$ the parameters $a_M$ and $b_M$ for bronze materials (Table 2) are identified with the equation (3.03).

**Table 2:** $a_M$ and $b_M$ for bronze materials

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<tr>
<td>$a_M$</td>
<td>0.268</td>
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<tr>
<td>$b_M$</td>
<td>-0.05</td>
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For the reason of the unknown tension-compression fatigue stress, $\sigma_{Wzd}$ (3.07) was specified out of the tensile strength $R_m$ and the tension-compression fatigue stress factor $f_{Wzd}$, which is recommended agreeing with the guideline using a value of 0.3.

$$\sigma_{Wzd} = f_{Wzd} \cdot R_m \tag{3.07}$$

All other required factors, especially the K-factors were determined according to the guideline. Especially the factor $K_{NL,E}$, who serve the non-linear elastic of the stress-strain behaviour and depends on material was determined for the considered bronze material by comparing with component tests.

$$K_{NL,E} \text{ (CuSn12Ni)} = 1.7$$

4. Validation of results

The comparison of the simulated worm gears had revealed a good correlation as well as the matching with the fatigue tests with real worm gears.

Based on these results the FKM guideline is possibly applicable for bronze materials. To ensure the results, further additional tests will be advantageous.

**References**

[1] DIN 3996 08-05, Tragfähigkeitsberechnung von Zylinderschneckengetrieben mit sich rechtwinklig kreuzenden Achsen, 2005


LIFETIME PREDICTION OF ELASTOMERS - A UNIFICATION OF THE FRACTURE MECHANICS AND THE (WOHLER) S-N-CONCEPT

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1. Introduction

In the last ten years elastomers were more frequently used in complex technical fields which require high performance. Therefore it is very important to have an instrument to estimate the service life of these technical parts. At the moment there are two classical methods for service life prediction. Both are more or less adapted from metal parts engineering [1, 2].

Fracture mechanics uses the characteristic values of dynamic crack propagation experiments to calculate the load cycles to failure for different load amplitudes. The other method, based on the concept of Wöhler [2], measures the lifetime of a sample for different load amplitudes (fatigue to failure tests, s-n curves). Both are useful for specific applications. But for rubber materials they can lead to different results for the same material. In this work an approach is shown to combine the two classical methods and an attempt is made to explain the different results. A new concept for lifetime prediction will be presented.

2. Materials and Methods

The idea, which will be explained, is a combination of the fracture mechanics concept with the Wöhler concept to reduce the experimental investigation of time and costs. Therefore the fundamental equations of Paris, Erdogan (a) are used and developed to an integral form (b) [3].

\[ \frac{dc}{dn} = B \cdot T^\beta \]  
\[ n = \frac{1}{(\beta-1) \cdot B \cdot (2kW)^\beta} \left( \frac{1}{c_0^{(\beta-1)}} - \frac{1}{c_n^{(\beta-1)}} \right) \]

Where T is the tear energy which can be calculated for different specimen geometries according to Thomas and Rivlin [4]. And ‘B’ and ‘β’ are characteristic material constants independent of geometry. ‘c_0’ and ‘c_n’ are the initial crack length and the crack length after n cycles.

Examples of Wöhler or s-n measurements on rubber materials is shown in Figure 2. The materials are based on a typical EPDM polymer filled with carbon black and sulphur cured. Two variations of the material are shown, one with pure carbon black dispersion due to a short mixing time (EPDM 1) and a typical dispersion due to a longer mixing time (EPDM 2).

The crack propagation behaviour of both materials was investigated on single edge notched (SEN) specimens with a Coesfeld[5] Tear Fatigue Analyzer. In order to determine the characteristic material constants ‘B’ and ‘β’ a minimum of three different strain amplitudes were used. The apparatus records the number of cycles and the crack length as well as the strain, the stress and the elastic strain energy density ‘W’. The crack propagation rate and the tear energy ‘T’ were evaluated in the initial stable crack propagation region.

‘Fatigue to failure’ experiments were carried out on dumbbell specimens in a MTS servo hydraulic tester under load control until complete rupture of the dumbbells (diameter 15 mm) [6].

An important prerequisite for fracture mechanics calculations is the knowledge of the initial crack or flaw sizes within the elastomer material. High-resolution x-ray computer tomography (CT) was used as a new and powerful method to characterize these structures. The analysis of the microstructures of compounds in a non-destructive way (dispersion analysis) is demonstrated.

3. Results

The CT analyses clearly reveal that the different mixing procedures lead to different stages of carbon black dispersion. Figure 1 shows the particle size distribution (specific number of particles per volume) evaluated by CT. Reference materials showed that most particles are carbon black agglomerates. The EPDM material 1 (short mixing time) contains more particles than EPDM 2 over the whole range of particle sizes investigated.
Note that EPDM 1 shows significantly higher numbers of particles especially for greater particle diameters in the range from 250 µm to 550 µm. This latter difference is argued to be the main reason for the different lifetime or fatigue to failure properties of EPDM 1 and EPDM 2 (factor: approx. 2.5) of Figure 2.

Fracture mechanics calculations or lifetime simulations of dumbbell specimens were carried out using equation (b). Note that ‘B’ and ‘β’ were taken from crack propagation experiments on SEN specimens and k and W from fatigue to failure tests on dumbbell specimens. ‘c₀’ was set to 15 mm which is the diameter of the dumbbells tested until complete rupture. The initial crack or particle size ‘c₀’ was tentatively set to 250 up to 550 µm.

This leads to different predictions of lifetimes which are well within the typical range of scatter of s-n- or Wöhler-curves (Fig. 2). The simulations show at the same time how critical only a few large particles can be. The best agreement between the simulation and the mean values of the fatigue to failure measurements is found for an initial crack size of 420 µm for EPDM 1 and 350 µm for EPDM 2. This shows that an average diameter of the greatest particles found in the materials are a good approach for the initial crack length in the lifetime simulation.

References
SYMPOSIUM SESSION V
THE EFFECT OF DWELL TIME VARIATION DURING MICROHARDNESS TESTING

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1. Introduction
Operation of microelectronic components at higher temperatures and for long periods is one of the major reasons for technical failure of solder joints due to creep effect. This effect also has an influence on the determination of local mechanical material properties via nanoindentation tests. It generally occurs during the dwell time of the indentation procedure. In order to analyze its influence on the measured values of Young’s modulus several nanoindentation tests were performed at different elevated temperatures for a eutectic lead-free solder material, namely Sn24Bi58, which has its melting point at 138°C. In these tests the dwell times were varied between 30 and 240 seconds.

2. Principles of Nanoindentation
The tests were performed with the nanotest-system from Micro Materials Ltd. The integrated indenter is a three-sided diamond pyramid known as Berkovich indenter and has a face angle of 78.9°. In order to determine the mechanical properties at elevated temperatures the indenter is modified by a hot-stage, where the indenter and the specimen can be heated up to a temperature of 500°C.

The displacement of the indenter in the specimen and the respective load are recorded during the nanoindentation process and plotted in a characteristic load vs. displacement curve (Fig. 1). This curve can be separated into three parts: loading, dwell time and unloading. According to Oliver and Pharr [1] the slope of the unloading curve at peak load

\[ S = \frac{dP}{dh} \]  

(1)

can be used to determine a so-called reduced Young’s modulus of the investigated sample material

\[ E_r = \sqrt{\frac{\pi}{A}} \frac{S}{2} = \frac{1}{2} \sqrt{\pi} \frac{dP}{dh} \]  

(2)

By means of Equation (3) Young’s modulus, \( E \), of the sample material is calculated by using indenter material properties as follows

\[ \frac{1}{E_r} = \frac{1 - \nu_i^2}{E} + \frac{1 - \nu_f^2}{E_f} \]  

(3)

where \( E_i = 1141 \text{ GPa} \) and \( \nu_i = 0.07 \) [2] denote Young’s modulus and Poisson’s ratio of the indenter material, respectively. \( \nu_f \) refers to Poisson’s ratio of the sample material and has to be known for the analysis. For solder materials the value for \( \nu \) can be assumed to be approximately 0.35.

Fig. 1: Schematic load-displacement curve

3. Experimental Setup and Performance
Hot-stage nanoindentation tests were performed at given temperatures of 30°C, 80°C and 130°C. The measured values on the sample surface were 29.5°C, 76°C and 125°C. Furthermore, the dwell times at each temperature level were adjusted to 30 s, 60 s, 120 s and 240 s, respectively. The specimens (including a fused silica specimen for calibration) were attached to the sample holder by means of a temperature resistant cement (Fig. 2). Twenty indents were made for every combination of temperature and dwell time in order to obtain a sufficient number of data points so that statistical errors are of little consequence.
4. Experimental Results

In Fig. 3 the effect of the dwell time on the unloading curves is presented for Sn42Bi58 at a surface temperature of 125°C. The curve of the 30s dwell time measurement (blue curve) shows considerable discrepancies to the ideal unloading behavior presented in Fig. 1. Due to non-ideal shape of the unloading curve it must be assumed that the analysis procedure of the nanoindentation software is not able to determine the correct gradient S of the unloading part, which, consequently, will lead to incorrect values for Young’s modulus. Furthermore it is clearly visible that an extended dwell time (up to a factor of eight of its initial value) reduces the influence of creep effects on the shape of the unloading part and the behavior becomes closer to the ideal one.

A comparison of our experimentally determined values of Young's modulus with the literature [3] is shown in Fig. 4. It shows results for Young’s modulus that were generated automatically with the evaluation software of the nanoindentation system. The results show that the values obtained by using longer dwell times are closer to literature data when compared to those obtained at shorter dwell times. Within accuracy of the measurements the beneficial effect of longer dwell times becomes also obvious for the 76°C results. However, at 125°C (homologous temperature = 0.96) our results differ considerably from the literature. This could be related to the fact that at this high temperature the material becomes extremely soft and the penetration of the indenter often exceeds its calibrated measurement range, which ends nearly at 1300 nm. For this reason only some measurements result in numerical values for Young’s modulus and the accuracy of these values cannot be guaranteed. In addition to the positive effect on the average values of E by prolonging the dwell times the increase also influences the standard deviation of the experimental results. At 240 s of dwell time the deviations from the average values are considerably smaller than at 30 s.

References

IDENTIFICATION OF THE YIELD SURFACE FOR SHEET STEEL USING AN OPTICAL MEASUREMENT SYSTEM

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1. Introduction

The usage of full-field deformation data from experiments for the identification of material parameters is a topic of current research. The continuous improvement of optical measurement systems leads to promising results in parameter identification and optimization [1]. With the high amount of data points the displacement and deformation field is not only a mean value but can be resolved locally which leads to a higher accuracy of the identified parameters.

The goal of this research is to get reliable results from the full-field measurement data through a Finite Element Model Updating Method with a lower amount of experiments compared to taking the yield values and Lankford coefficients for the characterization of the yield surface of sheet steel.

2. Experimental Characterization

In this work a deep-drawing sheet steel (DC04) is characterized, which was fabricated in a cold-rolling process. Due to this method of production sheet steel shows orthotropic material behaviour. To verify this fact uni- and biaxial tension and compression experiments were performed (fig. 1).

![Fig. 1: a) biaxial tension and compression specimen, b) uniaxial compression specimen](image)

The uniaxial specimens were tested with electro-mechanical machines and the biaxial ones with a hydraulic machine. In all these experiments the deformation was recorded with an optical full-field measurement system.

Assuming a homogeneous stress and deformation distribution in the measuring area of the specimen, the mean yield stress can be plotted in a stress-stress diagram (fig. 2).

![Fig. 2: Experimental Yield Surface](image)

Hill’s 48 yield surface in fig. 2 is calculated by utilizing yield values and Lankford coefficients. These parameters were identified through uniaxial tensile tests with specimen in three different angles to the rolling direction of the sheet (0°, 45° and 90°) [2]. The “Best-Fit” yield surface is obtained through a Least-Square Fit, based on the Galerkin-Method, see [3]. Input parameters are the experimentally determined yield values of all uni- and biaxial tests.

Comparing both yield surfaces a discrepancy especially in the biaxial region can be found. To be able to get better fitting material parameters a numerical parameter optimization is utilized.

3. Parameter Optimization

The identification is set up for two different experiments: the uni- and the biaxial tension test. The optimization is done by running Finite Element (FE) Simulations and iteratively changing the material parameters until the difference of the numerical results to the experimental ones is minimal.

Models of the uni- and biaxial specimen are built up and discretized with four-node, bilinear, isoparametric, plane stress quadrilateral elements. Symmetric boundary conditions are used to reduce the total amount of degrees of freedom. The load on the uniaxial specimen is applied through a displacement boundary condition and
the biaxial model is loaded with forces. As material model an anisotropic elasto-plastic behaviour with Hill’s 48 yield surface and Hockett-Sherby hardening is chosen.

The data from the optical measurement system has to be preprocessed for the parameter optimization. The software lays a grid on the tested specimen and calculates the displacement and deformation for all intersection points with Digital Image Correlation (DIC). To get the data values at the nodes of the FE mesh an interpolation routine is used. The loads for the simulation are taken from the experiments.

To verify the convergence of the optimization two algorithms (gradient-free Nelder-Mead Simplex and Levenberg-Marquardt with gradient through Finite Differences) and different initial points were utilized. The objective function is a weighed least-square sum of the differences of the displacement values at specified optimization points and of the reaction forces, between simulation and experiments (fig. 3).

Like in the uniaxial tensile experiments for each angle to the rolling direction (0°, 45° and 90°) three test specimens are taken for the simulation. The biaxial run takes the measured data from three experiments.

4. Identification Results

Figure 4 shows the results of the optimization process. As expected both identified yield surfaces fit the experimentally measured data better than the Hill 48 yield surface, which was generated from the uniaxial tensile yield values and the Lankford coefficients.

The uniaxial result shows a better correlation to the experimental data at the uniaxial data points for 0° and 90°, but the biaxial state cannot be represented well enough by the 45° specimens. The optimized biaxial yield surface shows a good fit in the biaxial area but does not perfectly fit at the uniaxial points. Both identified yield surfaces can be improved by implementing an anisotropic hardening function.

The convergence of the process is proven as the two utilized optimization algorithms and the different initial starting points lead to the same result. Even though the gradient for the Levenberg-Marquardt algorithm is determined through Finite Differences its performance is much better.

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References

EXPERIMENTAL STUDY OF A COMPOSITE BEAM LOADED IN FOUR POINTS BENDING TEST

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1. Introduction
In this paper the authors present an analytical and experimental study for determining the Young’s modulus in case of a composite beam (aluminum and wood-spruce) fig.1 and fig.2, loaded to pure bending. For the experimental study of displacements are used displacements transducers (WA20MM - HBM) and Digital Image Correlation (DIC) method. For spruce beam, displacements had determined for two cases: first, when the load is applied perpendicular on fibers, and the second, when the load is applied along the fibers.

2. Experimental analysis and the obtained results
The beam is simply supported, being subjected to four points bending, with variable forces (fig.1).

In fig.3 is presented the experimental setup, with the following elements: 1- force transducer U2B 10kN-HBM; 2- loading system; 3- beam; 4- displacement transducers WA20mm-HBM; 5- supports; 6- illumination system (DIC); 7- video cameras (DIC); CATMAN EASY-HBM software; 9- data acquisition system Spider8 (HBM).

For the analytic calculus of the Young’s modulus, has used the following relations:

a) spruce and aluminum beam:

$$E = \frac{F \cdot a \cdot (3 \cdot l^2 - 4 \cdot a^2)}{24 \cdot \nu_1 \cdot l_z}$$

where: E- Young’s modulus, a- distance between the support and the load F/2; l- distance between supports; $$\nu_1$$- the value of displacements in points 1, 2 and 3, determined by transducers and DIC method (fig.1); $$l_z$$- the inertia axial moment which depends by the beam section ($$I_{z,Al}=13101\text{mm}^2$$; $$I_{z,spruce}=19450.443\text{mm}^2$$).

b) composite beam:

$$\varepsilon = \frac{\nu_2 \cdot H}{\frac{l^2}{4} - \frac{a^2}{3}}$$

where, $$\varepsilon$$- strain, $$\nu_2$$- the value of displacements determined by two experimental methods, in point 2 (fig.1); H- beam section height.

The Young’s modulus had determined by Hook’s equation, thus:

$$E = \frac{\sigma_{max}}{\varepsilon}$$

Having in view the obtained results, had realised the diagrams from fig.4 and fig.5, which represents the variation of displacements, respectively of Young’s modulus, obtained by the two experimental methods, according to the applied forces. Also, in the figures are given the average values of the Young’s modulus.
Fig. 4: The variation of displacements obtained by a) transducers and b) DIC, where: SHOF, SVOF-Spruce Horizontally and Vertically Oriented Fibres; CHOF, CVOF- Composite Horizontally and Vertically Oriented Fibres.

Fig. 5: The variations of the Young’s modulus obtained by transducers

Fig. 6: The variations of the Young’s modulus obtained by DIC

3. Conclusion

In this paper had presented the experimental study of a composite beam loaded to bending in four points, and had determined the displacements on Y axis, then analytic, the Young’s modulus.

Having in view the diagrams from fig.4 and fig.5 can conclude the following:
- experimentally, had obtained an approximate linear representation, in elastic domain, of displacements and of Young’s modulus;
- the composite beam presents a rigid behavior when the load is applied along the fibers.

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References
DIC MEASUREMENTS – A COMPARISON OF DIFFERENT METHODS TO EVALUATE THREE-DIMENSIONAL DEFORMATION STATES

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1. Introduction

More and more optical 3d deformation measurement systems are used to analyse material and component behaviour. Obvious advantages in the analyses - especially at high strain deformation states, where nonlinearity caused by material behaviour and kinematics, anisotropy or inhomogeneity plays a role - are pushing these methods. Nevertheless DIC-based systems only serve to observe surfaces of specimens or components. For the determination of a full 3d deformation state, there is a need of information about a material point behind the observed surface. Due to this fact it is only possible to get either deformation states relative to a local coordinate system of a surface element, or to determine the full 3d deformation state relative to a global coordinate system by making assumptions in thickness direction. The aim of this paper is to give a general view of how deformation states can be evaluated based on raw point coordinates.

2. Alternative one (global system)

Consider an arbitrary continuum body embedded in a 3-dimensional Euclidean space in different configurations at different times (Fig. 1). The position of its particles in the reference configuration (at time t=0) is described by the vector \( \vec{X}_i \). \( \vec{x}_i \) refers to the position of the particle \( i \) in the current configuration (at time \( t \)). Here the points \( A, B \) and \( D \) are related to particles at the surface of the continuum (black). Point \( C \) is referred to a particle inside of the body (red). Assuming a linear displacement function, the deformation gradient \( F(X,t) = \text{Grad } u + I \) can be written as follows. Here \( x, y \) and \( z \) are related to the axes \( X_1, X_2 \) and \( X_3 \). Indices in capital letters are the components of the point in the reference configuration, lower case letters refer to the current configuration.

\[
F(X,t) = \begin{bmatrix}
(x_a - x_d) & (x_d - x_a) & (x_c - x_d) - (x_c - x_a) \\
(y_a - y_d) & (y_d - y_a) & (y_c - y_d) - (y_c - y_a) \\
(z_a - z_d) & (z_d - z_a) & (z_c - z_d) - (z_c - z_a)
\end{bmatrix}
\]

A constant \( F \) can only map a parallelepiped to another parallelepiped. This means that the edges will always be lines, and opposed edges are always parallel. That is why only four points are needed to completely describe the volume element.

\[
\begin{align*}
\Omega_0 & \quad \text{Zeit } t_0 = 0 \\
\Omega & \quad \text{Zeit } t
\end{align*}
\]

Fig. 1: continuum body

As you can see, six out of nine components of \( F \) can directly be calculated. With these two columns it is possible to directly obtain four components of the right Cauchy Green Tensor \( C = F^T F \).

The deformation gradient can also be seen as a linear mapping of a vector. This mapping must hold for all three vectors that describe the parallelepiped. Two of the vectors \( \vec{AB}, \vec{AD} \) are known in both configurations, so there are six equations but nine unknowns. To derive the missing three equations it is necessary to make a hypothesis about the position of the points \( C \) and \( c \).

A simple strategy is to assume that the vector \( \vec{AC} \) is perpendicular to \( \vec{AB} \) and \( \vec{AD} \) and has the...
length t. We then further restrict ourselves by stating that $\overrightarrow{ac}$ is perpendicular to $\overrightarrow{ab}$ and $\overrightarrow{ad}$. In fact, this assumption would completely agree with the realistic situation if no shear deformation in thickness direction took place.

Above assumptions lead to

$$X_3 := \frac{X_1 \times X_2}{\|X_1 \times X_2\|}, X_3 := \frac{X_3}{\|X_3\|} \lambda x_3$$

where $\lambda x_3$ represents the stretch in thickness direction.

A plane strain state is given, when the material vector does not change its length ($\lambda x_3 = 1$).

For other assumptions, one first has to calculate the first two eigenvalues $\varepsilon_1, \varepsilon_2$ of $C$ out of the first four directly computed components of $C$.

These are the first two squares of the main stretches. Assuming incompressibility the third principal stretch is calculated by $\lambda x_3 = \frac{1}{\sqrt{\varepsilon_1 \cdot \varepsilon_2}}$.

Or the stretch in thickness direction could be equal to the minor stretch value for orthogonal material behavior $\lambda x_3 = \sqrt{\varepsilon_2}$.

Using one of these choices it is possible to obtain the last missing vector to complete the linear system of equations.

Advantages of this procedure are the easy handling of assumptions and only very few calculation steps.

3. Alternative two (local systems)

A second alternative to validate a three dimensional deformation state of surfaces can be done by creating local coordinate systems. This can be achieved by reducing the 3-dimensional problem by one dimension. There are only three points needed to compute the full 2d deformation gradient. The local coordinate systems for both configurations must be parallel to the tangential surface, as shown in Fig. 2.

It is useful to create orthonormal local systems. One can imagine that only the direction of the local 3-direction is obvious - the normal to the tangential surface. The choice of the local 1- and 2-axis defines the basis for the right Cauchy-Green tensor in each point. Obviously these directions vary from point to point. To compute quantities which refer to only one system one has to transform the basis of the local deformation gradient. To do so, one has to expand local $F$ by one dimension, because the 3-dimensional transformation matrices are $3 \times 3$, the local deformation gradient is $2 \times 2$. By expanding the local $F$, the assumption in thickness direction comes into play. When one expands $F$ like

$$F''_{ab} = \begin{bmatrix} F''_{ab11} & F''_{ab12} & 0 \\ F''_{ab21} & F''_{ab22} & 0 \\ 0 & 0 & \lambda x_3 \end{bmatrix}$$

the same assumptions are taken as described before. Please note that one needs to switch both bases of $F$ by

$$F = F''_{ab} T''_{ia} T''_{ib} e_i \otimes e_j$$

with

$$e'_a = T'_i e_i, e'_b = T''_{ib} e_j$$

The advantage of this alternative is that both, local and global quantities can be calculated. Both alternatives lead under the same assumptions to the same global values of all kinematic tensors.
RESEARCH AND ACADEMIC EXPLORATION ABOUT OPTIMAL ATTITUDES OF DENTAL FILLING MATERIALS

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1. Introduction

Nowadays, dental filling materials with fizionomic properties are mostly used in dental care. Due to this, more and more products, which we know quite little about, appear on the market. During the curing process they all have certain polymerization shrinkage. Most of the dental practitioners believe that polymerization shrinkage is the causing mechanism of poor marginal adaptation between the two surfaces (tooth-filling material), ultimately leading to micro leakage and appearance of secondary caries lesions. Although this misconception is in clear contradiction with the facts experienced in dental practice it is widely spread. Our purpose is to demonstrate that poor marginal adaptation is caused mainly by the different elastic modules of the tooth and filling materials, and its consequences. We also wanted to categorize some of the most widely spread dental filling materials of our region from this point of view. This is important for us, because the least elastic material will exercise the smallest amounts of horizontal forces on the walls of the cavity during its deformation, in response of the vertical masticator forces. Summarising these, the dental filling material with the highest Young's modulus will not fracture the tooth's walls, and will not provoke micro-cracks in the tooth's tissues.

2. Experimental Setup and strategy

In order to establish the displacement fields the authors used an ESPI/Shearography System (ISI-Sys GmbH, Germany). The system allows a high-accuracy (with some nanometres resolution) evaluation of the displacements. In this sense the so-called reference plate method was applied. It is well-known that the image of this small, unloaded plate, superposed by shearing over the tested (loaded) specimen’s image offers a good and high-sensitivity strain analysis. Were subjected to uni-axial compression not only small cylindrical specimens (12 mm hight and 10 mm dia.) manufactured from different kind of dental filling materials, but also some real filled teeth pares with these materials.

In this sense, the authors conceived and manufactured some original loading devices.

Fig. 1: Experimental setup

In figure 1 is shown the experimental setup. The small reference plate 1 (having width 5 mm) is superposed by shearing over the tested specimen’s nearest site 2. The laser diodes 3 and 4 assure a good and equal illumination of the observed surface. They are fixed on the high-stiffened polycarbonate rods 7, 8. The Michelson Interferometer 5 and the 4 Mpx CCD cameras 6 are disposed in normal direction to the object. The distances are given in mm.

3. Experimental Results

In these experiences were tested 2 types of filling materials: composites (TE-Econom, Carisma, Ex-tra Fil), respectively glass-ionomers (Fuji Gp IX, Ketach Molar, Ionofil +). From all of them were manufactured both small cylinder specimens and filled teeth assemblies. In the following figures are illustrated both the cylinder-
shape specimens’ analysis and the filled teeth ones, too.

**Fig. 2:** Cylindrical specimen #1, Filtered data

**Fig. 3:** Cylindrical specimen #1, Evaluated data

**4. Final Remarks**

In these investigations the authors examined mainly the transversal elongation and its effect on the tooth.

The greater the lateral swelling of the filling material it is (in function of the compressive force exercised on it), the more it resorts the tooth's tissues (in fact the surrounding walls of the cavity in which the filling material is placed), mainly on bending. The aim of the research was to categorise and to evaluate from this point of view some of these often used dental filling materials.

Based on the result of the experiments, in the next period, the authors intend to perform some FEM analysis in order to establish the stress-field of the filled teeth.

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**References**


NUMERICAL AND EXPERIMENTAL ANALYSIS OF THE STATE OF STRESSES OF THE FEMORAL NECK – PLANE MODELING

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1. Introduction

The femoral bone is positioned between the hip joint and the knee joint, the upper extremity taking on the bodies weight that is transmitted by the vertebral column to the pelvis (Fig.1) [1].

Fig. 1: Direction of the body weight to the femur [1]

The femoral bone fractures occur in the femoral neck region. The paper wants to show the state of stresses and the displacements that take place during the compression load in the femoral neck area – plane modelling.

Were approached three cases: 1) the femoral neck is not sectioned; 2) in the fractured area two screws are introduced, both having equal diameters; 3) the reconstruction is achieved with 3 screws of different diameters.

The study uses finite element method (FDM) and Digital Correlation System (DIC).

2. Numerical analysis

In Figure 2 is presented the meshing pattern (triangular finite elements), bearing and load. For the three cases reminded earlier we have made a study that highlights the equivalent stresses distribution (Fig.3).

Fig.2: Plane model: a) without section b) two screws c) three screws

Fig.3: Tresca equivalent stresses distribution in the femoral neck section: a) model without section b) two screws c) three screws

3. Experimental analysis

For the experimental study we used DIC (Fig.4). The material chosen to make the plane
model of the upper extremity of the femoral bone is epoxy resin.

![Experimental setup](image)

**Fig.4:** Experimental setup: a) Instron 3366-1tf testing machine; b) Dantec Dynamics Q-400 DIC system; c) the point where the displacement is measured

Figure 5 represents the displacement variation (RDM and DIC) of O in keeping with the force that is applied. O lies on the femoral heads outline (Fig.6).

**Fig.5:** The displacement variation in vertical plane of O in keeping with the force: a) model without section b) with two screws c) with three screws

4. Conclusions

From the study conducted we can conclude the following:

− O’s displacements in keeping with the Y axes record a linear variation and the relative errors between the numerical results (RDM) and the experimental ones (DIC) are under 6 percent.

− The numerical and experimental analysis identifies the area were the load is maximum, which is the femoral neck area.

− In the cases of the models with two and three screws the maximum stresses appear in the lower screws.

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ANALYTICAL STUDY OF STRAIN’S RANDOM ERROR ON RESIDUAL STRESSES CALCULATED BY HOLE DRILLING METHOD

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1. Introduction

The hole-drilling method is an effective and popular semi-destructive technique for residual stress (RS) measurement. It consists in drilling a very small hole into the specimen; consequently, RS relaxes in the hole and stresses in the surrounding region change causing strains also to change; a strain gage rosette, specifically designed and standardized measures these strains. Using special stress-strain relationships, the RS field is calculated from the measured strains. Stress calculations are extremely sensitive to errors in the measured strain: small strain measurement errors can cause significant variations in calculated stresses, particularly for stresses far from the surface [1]. This error sensitivity occurs because the strains are measured at the specimen surface, but the desired RS are inside the specimen. This paper presents an analysis of the influence of the strain measurement error on the computed stresses. Particular emphasis is placed on influence of both the number of total steps and the type of step increment. Both the Integral and Power series stress calculation methods are investigated, and their different responses to measurement errors are described.

2. Methodology

The Power series assumes that the unknown stress distribution is expandable into a power series: \( \sigma(h) = b_0 + b_1h + b_2h^2 + \ldots \)

Finite element calculations are used to compute series of coefficients \( \tilde{a}_k(h), \tilde{a}_k^2(h), \tilde{b}_k^q(h), \tilde{b}_k^2(h) \), corresponding to the strain responses when hole drilling into stress fields with power series variations with depth \( h \), i.e., \( \sigma^q(h) = 1, \quad \sigma^1(h) = h, \quad \sigma^2(h) = h^2 \), etc. These strain responses are then used as basis functions in a least-squares analysis of the measured strain relaxations. Only the function \( \tilde{a}^1(h), \tilde{b}^0(h) \), and \( \tilde{a}^1(h), \tilde{b}^1(h) \) are given because the hole drilling method is not well adapted to giving accurate values for more than the first two power series terms for stresses. For the same reason, the maximum depth below the surface is limited to \( 0.5 \, r_m \), where \( r_m \) is the radius of the gage circle. An advantage of the Power series method is that it is relatively robust numerically because the least-squares procedure used tends to smooth out the effects of random errors in the experimental strain data. This averaging effect is particularly effective when strain measurements are made at many hole depth increments. A limitation of the method is that it is suitable only for smoothly varying stress fields.

In the Integral method, the surface strain relief measured after completing hole depth step \( j \) depends on the RS that existed in the material originally contained in all the hole depth steps \( 1 \leq k \leq j \):

\[
j_j = \frac{1}{2} \sum_{i=0}^{a} \sum_{j=0}^{b} a_{ijk} \cos \theta i + 1 \sum_{i=0}^{a} \sum_{j=0}^{b} b_{ijk} \sin \theta i \frac{d \theta}{d h} \frac{d h}{d \theta}
\]

where \( \theta \) is the angle of strain gage from the x-axis. The calibration constants \( a_{jk} \) and \( b_{jk} \) indicate the relieved strains in a hole \( j \) steps deep, due to unit stresses within hole step \( k \). Numerical values of the calibration constants have been determined by finite element calculations for standard rosette patterns. Using the Integral method, stress calculations are effective when few hole depth steps are used. For large number of drilling steps, the calibration matrices \( \tilde{a} \) and \( \tilde{b} \) become numerically ill-conditioned: small errors in experimental measurements can cause much larger errors in calculated residual stresses. To reduce this effect, ASTM E837-08 and the H-Drill software adopt the Tikhonov regularization.

3. Experimental Plan

Several works dealing with the non uniform residual stress measurement by the hole drilling method [1] have emphasized its high sensitivity to errors in the measured data. In particular, the experimental practice shows that the major errors in the computed stresses are due to the strain measurement error, since the other errors are generally minor. For this reason, the present study
concentrates on the strain measurement error, which is the dominant error source. In particular we have considered a set of linear deformation \((\varepsilon_{1L}, \varepsilon_{2L}, \varepsilon_{3L})\), the same set both with random strain error included between ±3µε \((\varepsilon_{1L±3}, \varepsilon_{2L±3}, \varepsilon_{3L±3})\) and with random strain error included between ±15µε \((\varepsilon_{1L±15}, \varepsilon_{2L±15}, \varepsilon_{3L±15})\). The problem of optimizing the step distribution was considered by Schajer [2] and Stefanescu [3]. In order to provide indications for the optimal step selection we have considered both constant calculation step (20, 10, 4 in a total depth of 1 mm) and incremental calculation step (12, 10, 8, 6, 4 in a total depth of 1 mm).

The errors on the calculated stresses are obtained from the equations:

\[
E_{±3} = \frac{\sigma - \sigma_{±3}^*}{\sigma} \times 100
\]

\[
E_{±15} = \frac{\sigma - \sigma_{±15}^*}{\sigma} \times 100
\]

where \(\sigma\) is the stresses calculated considering the linear deformation, \(\sigma_{±3}^*\) and \(\sigma_{±15}^*\) are the stresses calculated considering the linear deformation with random strain error included between ±3µε and ±15µε respectively.

4. Experimental Results

Figure 1 shows the maximum errors for minimum residual stress calculated by means Integral method and Power series method of Restan [4] and H-Drill software [5]. Experimental data show that error on Integral Restan are higher than on Integral H-Drill, both in case of constant and incremental step. This result should be correlated to the Tikhonov regularization that is implemented by H-Drill software and not by Restan software. Moreover, it seems that when strain measurements are made at many hole depth increments, errors of Power series H-Drill are smaller than errors of Integral H-Drill; when few hole depth steps are used, errors of Power series H-Drill are larger than errors of Integral H-Drill. Using Restan software it seems that errors of Integral method are always larger than errors of Power series method. Finally it seems that there isn’t difference between constant and incremental calculation step.

References


SYMPOSIUM SESSION VI
CHARACTERISATION OF SURFACE ACOUSTIC WAVE SENSORS FOR STRAIN MEASUREMENT

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1. Introduction

Strain gauges are applied in a wide industrial range. Sometimes, their application can be very difficult or even impossible due to a lack of space or the requirement of an electronic connection with a processing unit. To avoid these problems, the SAW Components Dresden GmbH has developed a new sensor system for strain measurements. It is capable to transmit the measured signal directly from the sensor to the processing unit without any need of wires. In comparison to the strain gauge technology, the deformation on the components surface is not determined by a change of the sensors electrical resistance. Instead the SAW device measurement principle is based on the propagation of surface acoustic waves between two interdigital electrodes. The resonance frequency of the standing wave can be determined using an interdigital transducer. This resonance frequency is directly related to the tension of the sensor and decreases with increasing strain. The coupling factor between the modification of the resonance frequency and the tension of the component, the sensor is mounted to, can be described by the sensitivity, similar to the gauge factor of strain gauges.

The sensitivity of the SAW sensors is mainly affected by the transmission of strain in the intermediate layer between component and substrate as well as the strain transmission of the piezoelectric crystal itself.

2. Numerical Analysis

The strain transmission of the sensor can be determined using different methods. One of them is the Finite Element Analysis (FEA). In addition to the mounting method, the strain transmission of the SAW sensor also depends on its geometry. Therefore the influence of the length, width and thickness of the carrier substrate has to be analyzed. By varying one of these geometric values, the other two geometric dimensions, the thickness of the intermediate layer and the sensor structure itself are kept constant, Fig.1. During the FEA the length of the whole sensor varies between 2700 µm and 6000 µm, its width between 630 µm and 2700 µm and its thickness between 100 µm and 500 µm. The sensitivity of the sensor increases with an increasing length and a decreasing thickness of the carrier substrate. The width of the sensor has no significant effect at all.

Based on these results, a sensor twice as long and half as thick as the actual available sensors pass more than 96 % of the components strain to the sensor geometry, which is comparable to the commonly used strain gauges technology [1]. This good strain transmission also leads to a higher sensitivity of the SAW sensor system.

3. Experiment

In addition to the optimization of the sensor geometry, the influence of the different application methods has to be characterized as well. Among other methods, the SAW sensor can be affixed like a strain gauge using a plane bond. This paper is focused on mounting the SAW sensor analogues to strain gauges without (a) and with steel bands (b), Fig.2.
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According to the literature different experimental setups are available. Due to the advantages of pure bending, four point bending is a suitable experimental approach to determine the sensitivity \([2]\) of the SAW sensors. Therefore a strain gauge and a SAW sensor are applied to a steel beam, Fig.3.

The strain gauge is used as reference and measures the strain in the outer fiber of the steel beam. Comparing the measurement results of the SAW sensor and the strain gauge against each other, the sensitivity can be estimated using

\[
S = \frac{\Delta f}{f_0} \cdot \frac{1}{\varepsilon_{\text{strain gauge}}}.
\]

4. Results

Referring to the experimental results, the sensitivities of the currently available SAW sensors are much smaller than for commonly used foil type strain gauges. While the sensitivity does not depend on the mounting method and loading of the sensor, these two parameters mainly affect the applicability of the sensor system. In comparison to fixing the sensor in an analogous manner to strain gauges, mounting the sensor with steel bands has one significant advantage. For compressions up to 2000 \(\mu\text{m/m}\), its sensitivity is almost constant, Fig 4.

The sensitivity rapidly decreases under tension for both mounting methods, because the bond between the sensor and the component as well as the bond between the sensor and the steel bands fails.

5. Outlook

Current research is focused on a new application method, where of the sensor is pre-loaded by an additional centre key and embedded in a cavity. First analyses are quite budding, because the sensitivity of the whole sensor system can be increased to about three, which is also high compared to foil strain gauges.

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INVESTIGATION IN DEVELOPMENT OF EMBROIDERED STRAIN MEASUREMENT SENSORS

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1. Introduction

The integration of electronic units, sensors and actuators into complex function-oriented systems is one key point in development of intelligent lightweight composites and lightweight structures. A wide range of materials are available for that purpose and can be used to create active structures with selective properties. Often, these complex systems are manually integrated into smart structures with prefabricated and individually designed layer materials. But, automated production is difficult.

Therefore, main aim of Department of Lightweight Structures and Polymer Technology (SLK) in cooperation with Competence Centre for Lightweight Structures (SLB) and the Lightweight Structures Engineering GmbH (LSE) is developing application-oriented technical solutions for series production. One result of this network is a technology for the fabrication of embroidered strain measurement sensors. These sensors are capable for a direct integration whilst the manufacturing of active lightweight structures and composites, for example fiber reinforced plastics.

2. Embroidery technology

Embroidering, micro system technology and polymers combined in a closed process chain is quite unusual so far. However the research results showed that there are novel and future orientated fields of applications by embroidery technology. This technology is actually well known in industrial applications like Tailored Fiber Placement (TFP) for selective reinforcement of high duty plastic systems.

In both ways, the TFP and embroidery method, fibers or wires can be places in individual patterns on flexible base materials. Furthermore, this technique is known for producing micro system structures in the field of flexible materials. Thin metallic wires (e.g. constantan® or isaohm®) or conductive coated or rather conductive yarn are used as sensor materials. Embroider techniques are used to attach the sensor material on a non-woven [1]. Figure 1 shows a larger scale image of that. The wire shown in this picture is positioned by Tailored Fiber Placement technologies and fixed with clearly visible purple yarn on a non-woven polymer. In principle the shape and dimension can be designed individually. At the moment the achievable resolution is about 0.8 mm.

Fig. 8: Embroidered sensor structures embedded in fiber reinforced plastics (above), production process (bottom left) and enlarged view of a sensor (bottom right)

3. Strain measurement

The total resistance is a very important parameter for strain sensors. Its value is key factor for power consumption by sensor system. Besides, typical resistances of 120 Ω, 350 Ω and 1 kΩ per sensor can be designed for any value between and
around that. Tolerance for series production is about ±10% at the moment. For small scale and laboratory application a tolerance of ±3% is achievable.

The demonstrated strain sensor has less priority in exact measurement of mechanical strains inside a component like common strain gauges. It is rather used to functionalize fiber reinforced structure components. For example it is possible to adapt a part with a sensor that gives information about current status (health monitoring). Hence the sensor is embedded it is protected against environmental influence like humidity. By choosing an appropriate geometry and adjusting resistance the sensor can be freely customized and fitted to almost any component.

A common bridge connection is used to analyze the sensor signal in the same way as for typical strain gauges [2]. If a standard value is chosen for resistance a usual industrial analysis unit can be applied.

4. Experimental setup

The gauge factor of that sensor was determined as well. Therefore the sensor was glued on another beam. That beam was deflected in a four-point bending experiments (see Fig. 5). The test was carried out according to the VDI/VDE GESA 2635 norm (compare [3]).

![Fig. 9: Experimental setup for the investigation of gauge factor](image)

The gauge factor is the proportion of the elongation of the sensor to its change in resistance. Due to the four-point bending experiment the deformation at the sensor area is clearly defined. The gauge factor can be calculated as followed:

\[
k = \frac{\Delta R / R_{0,\text{pol}} + \Delta R / R_{0,\text{rel}}}{\varepsilon_{M,\text{pol}} + \varepsilon_{M,\text{rel}}}
\]

5. Experimental results

The experiment results are shown in Fig. 6. All tested sensors provide a nearly constant gauge factor over a width elongation range.

![Fig. 10: Measured gauge factor and standard deviation for 10 investigated sensors](image)

Acknowledgements: The authors thank the Federal Ministry of Education and Research (BMBF) for the financial support.

References


[3] VDI/VDE/GESA 2635: Experimental structure analysis Metallic bonded resistance strain gages Characteristics and test conditions
ELASTIC BUCKLING OF 2D CELLULAR STRUCTURES

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1. Introduction

Recent experimental [1] and numerical [2] studies have uncovered a fascinating pattern switching mechanism in 2D cellular structures under uniaxial compression. A square lattice of initially circular voids transforms into an array of mutually orthogonal ellipses as a result of a buckling instability triggered at a critical stress value. This reversible, repeatable phenomenon occurs globally throughout the sample and has been observed at the sub-micron level [3]. This has led to the manufacture of tunable optical devices which utilise this geometric switching to alter photonic band gaps in periodic structures [4]. The switch also causes a counter-intuitive auxetic effect [5].

Traditional studies of 2D cellular structures focus on the role of void size relative to the lattice on the response of these structures to compression. Here, we present the results of experiments which probe the influence of this parameter on 2D cellular structures possessing circular voids. These findings are compared with a simple spring-link model which uses potential energy minimization techniques to predict the collapse of the structure, and hence the onset of pattern switching, as a result of uni-axial compression.

2. Experimental Method

2D cellular structures possessing circular voids arranged on a square lattice are manufactured using the addition-curing silicone rubber “Sil AD Spezial” (Feguramed GmbH). A mixture of two fluids is poured into moulds comprising of 16 cylindrical inclusions (diameter 8.8mm) arranged on a 4 by 4 square lattice and bound by an adjustable aluminium perimeter (Fig. 1a). This is then allowed to set for one hour before removal from the mould. By varying the inter-hole distance, the relative volume fraction of the sample is changed.

Fig. 1: a) The mould. b) A specimen stands in the housing on the “Instron 5569” machine.

Specimens are subjected to uniaxial compression tests using an “Instron 5569” machine. (Fig. 1b). The sample stands in a housing which prevents out of plane buckling and flour is used to reduce frictional effects. A perspex sheet is clamped to a 1kN load cell (#2525-806) in order to apply load to the surface of the sample.

3. Experimental Results and Analysis

A marked change in the shape of the voids is observed as the honeycomb is compressed. Initially the cells remain globally identical (Fig 2a). As the strain increases, the pattern switch to an array of mutually orthogonal ellipses is observed (Fig 2b).

Fig 2: The honeycomb is shown prior (a) after (b) pattern switching.

The load-displacement data collected during experiments is converted into stress-strain data. The data take the form associated with cellular solids under compression, an initial linear regime which is followed by a plateau region of near-constant stress as the sample collapses. The
critical stress value is determined by linear extrapolation techniques. A sample data set is shown in Figure 3.

Fig. 3: The experimental stress-strain curve shows an initially linear compression regime followed by a plateau phase. The dotted lines mark linear extrapolation to find the critical stress value.

4. Discrete Model

The discrete model of the specimen is scaffold of rigid bars of length, \( l \), equal to the inter-hole spacing. The bars are joined to one another by hinges, and where horizontal and vertical bars cross, the relative angle between the two is fixed to 90 degrees (Fig 4a). When the discrete model buckles, each rod is deflected by an angle \( \alpha \) from its original orientation (Fig 4b).

The bending stiffness of the structure is represented by the existence of a rotational spring, stiffness \( \rho \), at each hinge. The stiffness of each spring is calculated by considering the second moment of inertia of a tapered rod and its relationship to resistance to bending when compressed uniaxially. \( \rho \) is determined by the equation

\[
\rho = E \left( \frac{1}{l} \int_0^l \frac{1}{I(x)} \, dx \right)^{-1}
\]

where \( l \) is the inter-hole distance, \( E \) is the Young's modulus of the elastomer and \( I(x) \) is the second moment of inertia of a tapered beam.

By considering the potential energy of the system and the stability of the trivial (\( \alpha = 0 \)) state it is possible to predict the critical stress as

\[
\sigma_{CR} = \frac{8\rho}{w^2 l^2}
\]

where \( w \) is the width of the honeycomb, and \( \rho \) and \( l \) assume their previous definitions. It is possible to compare the predictions of this simple bending dominated model to those from experiments to gain further insight into the response of 2D cellular solids to compression.

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References

CHARACTERISATION OF NEW EMBEDDED EMBROIDERED SENSORS FOR STRAIN MEASUREMENTS IN COMPOSITE MATERIALS

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1. Introduction

The structural integration of new embedded embroidered sensors (EES) in fibre reinforced plastics (FRP) has some advantages by comparison to other measurement solutions like normal strain gauges or fibre bragg gratings (FBG). The ability to produce them in mass production and the less expensive data acquisition systems lead to lower costs by contrast with FBG. In comparison to strain gauges there is no weakening of the FRP after integrating the sensor.

For the use of new embedded embroidered sensors (fig. 1) knowledge about their characteristics is very important. Therefore some theoretical considerations and practical test were realised.

![Fig. 1: New embedded embroidered sensor](image)

2. Design of the Sensor and the Test Specimen

For the experiments the sensors were integrated into different laminates (tab. 1) made of glass fibre reinforced plastics (GFRP). They were placed in the middle of the laminates (Fig. 2). Furthermore there were three sensors in each specimen, one along the force direction, one transeverse and one in 45° (Fig. 3).

The position for the contact wires was placed outside the measurement area. The connection between the sensors and the contacts were made of copper wires. These wires are stretched in the experiments, but their influence on the signal can be neglected.

![Fig. 2: Integration of EES in GFRP](image)

![Fig. 3: Specimen with three sensors](image)

3. Experimental Setup and Results

To characterise the new embedded embroidered sensor, the specimen were tensioned and the reproducibility of the signal was investigated. Furthermore longitudinal strain of the samples was 1%. The results showed that the reproducibility of the sensors was very good. The standard deviation of the signal was ±1,74%.

<table>
<thead>
<tr>
<th>Tab. 1: Different types of test specimen</th>
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<tbody>
<tr>
<td>type</td>
</tr>
<tr>
<td>Unidirectional (UD)</td>
</tr>
<tr>
<td>Bidirectional (BD)</td>
</tr>
<tr>
<td>Quasi isotropic (QI)</td>
</tr>
<tr>
<td>Antisymmetric (AS)</td>
</tr>
<tr>
<td>General (GL)</td>
</tr>
</tbody>
</table>
4. Transverse Sensitivity

The investigations were focused on the transverse sensitivity. Although the experimental setup is not appropriate to get transverse sensitivity of the embroidered sensor the results can show its dimension. Concerning Wei-ming [1] the transverse sensitivity

\[ q = 0.0375 \pm 30\% \]

is determined by

\[ q = \frac{\nu_{mes} - \nu_{12, lam}}{\nu_{mes} \cdot \nu_{12, lam} - 1} \]

with

\[ \nu_{mes} = \frac{\varepsilon_{mes, transverse}}{\varepsilon_{mes, lengthwise}} \]

The index mes indicates a measured value. The value of the Poisson’s ratio \( \nu_{12, lam} \) was calculated with the theoretical material constants of the single layers.

To get an overview about influences through the transverse sensitivity some theoretical considerations were made. Concerning Keil [2] the systematic error caused by the transverse sensitivity can be calculated with

\[ f = \frac{q(\nu_0 + m)}{1 - q \nu_0}, \]

where \( \nu_0 \) is the Poisson’s ratio of the reference material (used for determination of the gauge factor) and \( m \) is the relation between transverse and longitudinal strain. In case of uniaxial tension this relation is the Poisson’s ratio, but relating on the sensor coordinate system it has to be transformed (fig. 4).

The polar diagram (fig. 5) shows the measurement error depending on the sensor-stress-orientation for three different laminates. The measurement errors for UD- and QI-laminates are up to 15% and for BD-laminates up to 40%. The results of AS- and GL-laminates are similar.

Fig 5: Measurement error depending on the sensor-stress-orientation

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References
COMPARISON OF MECHANICAL PROPERTIES BETWEEN SINGLE AND MULTI LAMELLAR SAMPLES OF INTERVERTEBRAL DISC

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1. Introduction

Annulus fibrosus as an important element of structure of intervertebral disc play fundamental role in mechanical function of spine. Lamellar annulus fibrosus wall mainly consist of collagen fibers, water and proteoglycans [2,3]. Arrangement of the fibrous matrix in intervertebral disc decide to nonlinear and strongly anisotropic tensile behaviour of the anulus fibrosus [1,2,4].

The presented study, provide information about influence number of lamellar, structural organization and connection vertebral bone with anulus fibrosus at mechanical properties.

2. Material and Method

Nondegenerate intervertebral disc were collected from 4 pigs spine. To this research, were used 3 motion segments from thoracic section of spine (Th8-Th9, Th9-Th10, Th10-Th11). Specimens were assorted and frozen at -20°C until testing. Samples of several annulus fibrosus were cut from outer anterior margin of motion segments with vertebral bones (fig. 1). Single lamellar samples was isolated using a scalpel from several wall of annulus fibrosus.

Before mechanical testing samples were tested in 0.15 mol/l NaCl saline environment at room temperature. Dimensional of single and multi lamellar of intervertebral disc are summarized in Fig. 2.

<table>
<thead>
<tr>
<th>DIMENSIONAL OF ANNULUS FIBROSUS SAMPLES</th>
</tr>
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<tbody>
<tr>
<td>LENGTH [mm]</td>
</tr>
<tr>
<td>SINGLE 6,00±1,10</td>
</tr>
<tr>
<td>MULTI 5,25±0,87</td>
</tr>
<tr>
<td>WIDTH [mm]</td>
</tr>
<tr>
<td>SINGLE 16,00±1,41</td>
</tr>
<tr>
<td>MULTI 5,64±1,18</td>
</tr>
<tr>
<td>THICKNESS [mm]</td>
</tr>
<tr>
<td>SINGLE 4,83±0,98</td>
</tr>
<tr>
<td>MULTI 0,43±0,10</td>
</tr>
</tbody>
</table>

Fig. 2: Dimensional of single and multi annulus fibrosus samples

Samples of annulus fibrosus with vertebral bodies bone were examined using uniaxial tensile test. All samples were preconditioned with 5 cycles to 1mm displacement. After that, using MTS Synergie 100 testing machine samples were stretch with constant speed of 0,5mm/s to failure of samples.

Tensile characteristics of intervertebral disc were determined between single and multi lamellar samples of annulus fibrosus (fig. 3).

3. Experimental Results

These experimental results of nondegenerate intervertebral discs have shown difference
mechanical properties between single and multi lamellar samples of annulus fibrosus.

For characterization of mechanical behaviour parameters: stiffness coefficient (k), ultimate stress ($\sigma_{\text{MAX}}$) and Young’s modulus (E) were calculated from load-displacement and stress-strain curves.

For characterization of mechanical behaviour parameters: stiffness coefficient (k), ultimate stress ($\sigma_{\text{MAX}}$) and Young’s modulus (E) were calculated from load-displacement and stress-strain curves.

Fig. 4: The mechanical properties of annulus fibrosus: a) stiffness coefficient (k), b) ultimate stress ($\sigma_{\text{MAX}}$), c) Young’s modulus (E)

The higher value of stiffness coefficient (k) were for multi lamellar samples then single lamellar samples (Fig.4.a). Whereas, ultimate stress ($\sigma_{\text{MAX}}$) and Young’s modulus (E) of the single lamellar annulus fibrosus samples excised from thoracic spine was higher than multi lamellar annulus fibrosus (Fig.4.b,c).

4. Conclusion

Samples of outer wall of annulus fibrosus with two half of vertebral bodies have shown large influence elements of vertebral bones to mechanical properties. Stiffness coefficient were grown with number of lamellar annulus fibrosus but for multi lamellar samples the other mechanical properties (ultimate stress and Young’s modulus) were become smaller.

Several samples of annulus fibrosus were undergone destruction in effect of separation annulus fibrosus with endplate from vertebral bone.

This is to confirm, that connection between endplate and vertebral bone is important place and should be taken into consideration in mechanical testing of intervertebral disc.

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References


1. Introduction

The plastic deformation behavior of textured sheet metals under biaxial compression loading is mostly unknown, since the technical implementation of biaxial compression in experimental setups is challenging. Experimental data under biaxial compression loading of textured sheet metals may well be essential to describe their tension / compression asymmetry. There are yield criteria to account for this asymmetry, and for the Bauschinger effect – but accurate modeling relies on accurate experimental data. Various testing methods have been devised to complement conventional tensile testing. Yet there is no systematic work so far that describes the ‘third quadrant’ (σ−σ plane) under biaxial compressive loading. In this study, an experimental setup is presented that will help bridge this gap.

2. Experimental Setup

The biaxial/uniaxial compression setup is presented in Figure 1. The strain-controlled test rig can be used in a conventional universal testing machine. In this study, a Zwick UPM-1475 provided the compressive force. A four-columned housing guides the upper part to the bolted lower part to prevent tilting. Four independent plungers are located inside the housing (Fig. 2a). The movement of each plunger is controlled by four wedge-shaped parts inside the cap. Each plunger is guided in a channel. On the tip of each plunger, two pairs of strain gages are attached; one pair measures the elastic deformation due to deformation force, and the second pair (which is oriented at 90° to the main strain gage) is used for temperature compensation. Each plunger is calibrated for the investigated sheet thickness. The wedges inside the cap can be changed - this feature allows to use wedges with different angles in each direction, which in turn creates the possibility to initiate different strain ratios. In the study presented here, three sets of wedges were used with angles of 6.6°, 13.0° and 19.1°. To move the plungers simultaneously, a LabVIEW code was developed. The graphical user interface displays the live force of each plunger. A low elastic pre-stress of around 100 N in each direction is adjusted to clamp the specimen in a properly aligned position prior to testing.

Fig. 1: biaxial/uniaxial compression test rig

Fig. 2: a) plunger configuration b) specimen geometry and specimen with Speckle pattern

The strain measurement during the test is realized with a 2D DIC (digital image correlation) system. A mini CCD camera is placed under the plungers to detect the speckle pattern on the surface of the specimen (Fig. 2b). The speckle...
pattern is applied with an airbrush system to obtain a finely dispersed, homogeneous speckle pattern. In all compression tests discussed below, the strain rate was set to 10^{-3} \text{s}^{-1} and specimen thickness was 1 \text{mm}.

3. Experimental Results and Discussion

Figure 3 shows the stress-strain curves in uniaxial tension and compression, as well as for biaxial compression (loading path 1:1) for a sintered, quasi-isotropic material (with a nearly ideally isotropic texture coefficient of 1.027; further details will be presented elsewhere). The quasi-isotropic material was used to validate the specimen geometry and the test rig; there should be no tension compression asymmetry for this kind of material.

The material behaviour is in good agreement with what is expected for an isotropic material. For uniaxial tension and compression, the measured flow stress is identical. The biaxial load case with the same plunger velocity in each loading direction also shows the same flow stress up to a plastic strain of about 0.025. The variation in flow stress at higher plastic strains is most likely due to the increasing friction between the plungers and the specimen. Clearly, our novel test rig allows measuring initial flow stresses under uniaxial and biaxial compression loading on sheet metal material.

The experimental procedure was also applied to a typical engineering material, a deep drawn steel DC06. The material was annealed during manufacturing and ‘skin-passed’ to prevent stretcher strain marks in the forming process. A detailed discussion for the DC06 and two aluminium alloys are presented in [1, 2]. A comparison of the measured values with the non-quadratic yield criterion Hill 1990 [3] highlights a distinctive tension compression asymmetry, Fig. 4. Our new testing approach allows a detailed characterization of this asymmetry under biaxial loading conditions.

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References

DEVELOPMENT OF VISCO-ELASTO-PLASTIC MATERIAL MODEL FOR HUMAN TRABECULAR BONE USING NANOINDENTATION

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1. Introduction

Knowledge of trabecular bone behaviour under loading is very important for bone quality assessment. For numerical modelling of deformation behaviour of trabecular tissue it is necessary to establish a material model for single trabeculae. For visco-elasto-plastic material model used in this study have to determine eight material constants. Two elastic constants were obtained directly from nanoindentation test by Oliver-Pharr method [1]. Remaining material constants were identified from a Finite Element (FE) simulation of nanoindentation test [2].

2. Material and Methods

2.1 Nanoindentation test

A 3mm thick sample for the nanoindentation experiment was prepared from human (male donor, 72 ages) femoral head using precision saw (Isomet 1000, Buehler). The sample was cleaned in 1% Alconox detergent lotion and then grinded using polishing machine (LaboPol-4, Struers) with diamond grinding discs. The grinding procedure was finished with monocrystalline diamond suspension and final polishing was done using aluminum-oxide suspension with 0.05 µm grain size.

During the nanoindentation test a diamond indenter (Berkovich type) was pressed down into the sample surface. Load and penetration depth of the indenter was measured. Different loading rates, holding times and peak loads were applied. Various loading parameters were chosen for statistically significant fitting procedure of the FE simulation. About 300 indents with 10 µm grid size (fig. 1) were performed at different places of the sample (about 20 indents from the same location).

2.2 FE simulation of nanoindentation test

For FE simulation of the nanoindentation test a rotationally axisymmetric plane model was developed. The sharp Berkovich tip was replaced with equivalent cone and rounded due to use of nonlinear contact between the indenter and the sample. For the diamond indenter a pure elastic model with known Young’s modulus (Ei=1140GPa) and Poisson’s ratio (µi=0.04) was used. For trabecular bone material model with von Mises yield criterion and bilinear isotropic hardening with implicit creep was chosen. The model requires determining of eight material constants. Young’s modulus (Etrab) and Poisson’s ratio (µtrab) for elastic part yield stress (σy), tangent modulus (Etan) for plastic part and coefficients C1, C2, C3, C4 for implicit time hardening creep using Eq. 1.

\[ \dot{\varepsilon}_{cr} = C_1\sigma^2 C_2 e^{C_3/T} e^{-C_4/T} \]  

Where \( \dot{\varepsilon}_{cr} \) is equivalent creep strain, \( \sigma \) is equivalent stress, \( T \) is temperature and \( t \) is time at the end of a substep. Elastic constants were established directly from experiment, remaining six constants were determined by fitting procedure of nanoindentation curves.

2.3 Fitting procedure

Nanoindentation curves from experiment were sampled and obtained load values were prescribed
to model of indenter in each loadstep. Results of the FE simulation were compared with experimental ones by least squares method and R² error was calculated. Initial values of material constants were chosen and modified in each simulation using custom grid optimisation algorithm (described in the flowchart in fig. 2.) to minimize R².

![Flowchart of fitting procedure](image)

Fig. 2: Flowchart of fitting procedure

3. Results a discussion

Best material constants (Eₗₐₜ_best, Cₖ_best) for each indentation curve obtained from the optimisation procedure were compared with other resulting constants from curves of same indentation area. Although the indents came from a similar area (10 µm grid size), the identified constant weren’t exactly the same and determined appropriate ranges for material model of single trabecular are shown in Tab. 1. This discrepancy can be attributed to several influences (material, experimental, numerical). Trabecular bone is inhomogeneous biological material and its material properties in different locations can significantly vary. Influence should also simplify the numerical model due to countability. Nevertheless, presented optimization scheme is suitable for determination of parameters of visco-elastic-plastic material model for trabecular bone. This material model is needed for large-deformation analyses of trabecular bone [3].

<table>
<thead>
<tr>
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<tr>
<td>µₘₐₓ [-]</td>
<td>0.2</td>
<td>0.2</td>
</tr>
<tr>
<td>σₐ [MPa]</td>
<td>110-205</td>
<td>180-240</td>
</tr>
<tr>
<td>Eₗₐₜ [MPa]</td>
<td>1070-1340</td>
<td>950-1300</td>
</tr>
<tr>
<td>C₁ * e⁻¹³</td>
<td>5.2-8.57</td>
<td>6.2-8.39</td>
</tr>
<tr>
<td>C₂</td>
<td>3.98-4.87</td>
<td>2.95-3.83</td>
</tr>
<tr>
<td>C₃</td>
<td>0.65-0.9</td>
<td>0.3-0.54</td>
</tr>
<tr>
<td>C₄</td>
<td>0.32-0.37</td>
<td>0.36-0.38</td>
</tr>
</tbody>
</table>

*area[peak force [mN]/ holding time [s]/ loading rate [mN/min]]

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