Association of Metallurgical Engineers of Serbia
Faculty of Technology and Metallurgy, University of Belgrade
Serbian Foundrymen's Society
Metallurgical Academic Network of SEE Countries
Institute for Technology of Nuclear and Other Mineral Raw Materials
Institute of Chemistry, Technology and Metallurgy
Vinca Institute of Nuclear Sciences
Lola institute

MME SEE 2015

Metallurgical & Materials Engineering Congress of South-East Europe

PROCEEDINGS AND BOOK OF ABSTRACTS

Editor:

Marija Korać

June 3-5, 2015
Belgrade, Serbia

Editor:

Dr Marija Korać
Faculty of Technology and Metallurgy, University of Belgrade

Technical editor:

Department of Printing Engineering
Faculty of Technology and Metallurgy, University of Belgrade

Published by:

Association of Metallurgical Engineers of Serbia (AMES)

Circulation:

150 copies

Printed by:

Department of Printing Engineering Faculty of Technology and Metallurgy
Karnegijeva 4, POB 35-03
11 120 Belgrade, Serbia
Tel: +381 11 3370 492

ISBN 978-86-87183-27-8

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PREFACE

Metallurgical & Materials Engineering Congress of South-East Europe (MME-SEE 15) is a meeting of scientists, professionals and specialties working not only in the field of processing of metals and materials, but also those engaged in research related to the production, structure and property relationship and applications of modern materials.

Time has shown a strong need for interdisciplinary research in metallurgical and materials engineering. Therefore, in order to cover all research fields, Congress represents fusion of following scientific events: Balkan Conference of Metallurgy, Processing and Structure of Materials, Light metals and Composite materials and International Foundrymen Conference. Regional significance is supported by South East Europe Associations of Metallurgical Engineers, Balkan Union of Metallurgists and Chambers of Commerce of SEE Countries, and organized by Association of Metallurgical Engineers of Serbia, Serbian Foundrymen's Society and Metallurgical Academic Network of SEE Countries

The Congress brings together a wide range of related topics and presents the views from both academia and industry. Future of metal industry in South-East European countries, geology and minerals potentials for metallurgy production, new industrial achievements, developments and trends in extractive metallurgy, ferrous and nonferrous metals production, metal forming, casting, powder metallurgy, new and advanced materials, coating, galvanizing, corrosion and protection of materials, process control and modeling, recycling and waste minimization, nanotechnology, sustainable development, solvothermal synthesis, physical metallurgy and structure of materials, welding, environmental protection and education are all covered in the Proceedings.

The Scientific Committee hopes that the Congress will help to improve the knowledge on the symbiotic topics as processing and materials properties.

The Editor would like to thank the Scientific Committee, and the Secretariat - CONGREXPO d.o.o. and all those who helped in making the Congress a success.

The Congress is organized jointly by the Association of Metallurgical Engineers of Serbia, Faculty of Technology and Metallurgy, University of Belgrade, Serbian Foundrymen's Society, Metallurgical Academic Network of SEE Countries, Institute for Technology of Nuclear and Other Mineral Raw Materials, Institute of Chemistry, Technology and Metallurgy, Vinca Institute of Nuclear Sciences and Lola institute

Special thanks are due to the Ministry of Education, Science and Technological Development of the Republic of Serbia for the financial support of the Congress,

Editor

CALCULATION OF LOCAL CLEAVAGE FRACTURE STRESS IN A MEDIUM-CARBON V-MICROALLOYED STEEL USING FINITE ELEMENT ANALYSIS

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Abstract

Cleavage fracture of commercial medium-carbon V-microalloyed forging steel with predominantly acicular ferrite structure was studied by means of four-point bending testing at liquid nitrogen temperature in conjunction with finite element analysis. Both two-dimensional and three-dimensional finite element models were used for the purpose of comparison. Cleavage fracture was initiated by high plastic strains in the vicinity of notch tip. Strains calculated by three-dimensional modeling were higher up to 80% than for two-dimensional model. Calculated local cleavage fracture stresses rendered effective surface energy values of 49J/m^2 and 60J/m^2, for two-dimensional and three-dimensional numerical model, respectively. Both results are in good agreement with previously published data for medium carbon microalloyed steels.

Keywords: medium carbon V-microalloyed steel, acicular ferrite, critical fracture stress, finite element model.

Introduction

It was established that cleavage fracture of the microalloyed forging steels with ferrite-pearlite, bainite or martensite structure initiates by fracture of coarse second phase particles, commonly TiN particles [1–3], in the region of high stress intensification, such as peak stress in front of the notch on bend test samples. However, in microalloyed steels with acicular ferrite structure, cleavage fracture initiation is associated with plastic deformation at the notch tip, where stresses are relatively low [4–6]. Some authors proposed that, in addition to critical stress, critical plastic strain and critical stress triaxiality should be considered as additional criterions for cleavage fracture [4].

Critical parameters for cleavage fracture are determined by using finite element models (FEM) in conjunction with fractographic examinations of the notched samples tested by three- or four-point bending. Two-dimensional (2D) FEM with plane strain assumption are commonly used [1–3,7]. The question of validity of 2D FEM assuming plane strain condition arises in the case of strain induced cleavage fracture.

The aim of this investigation is to determine critical parameters of cleavage fracture by using three-dimensional (3D) FEM and to compare the results with plane strain 2D FEM, for medium carbon microalloyed steel with the structure of acicular ferrite.

Experimental Procedure

A commercial V-microalloyed medium carbon steel containing: 0.256%C, 0.416%Si, 1.451%Mn, 0.0113%P, 0.0112%S, 0.201%Cr, 0.149%Ni, 0.023%Mo, 0.099%V, 0.002%Ti, 0.038%Al, 0.183%Cu, 0.002%Nb, 0.0229%N, was tested in this work. As received hot rolled bars, 19mm in diameter, were given homogenization treatment at 1250°C for 4h followed by oil quenching. The samples were afterwards austenitized at 1250°C for 30 min and cooled at still air. In all heat treatments, argon was used as a protective atmosphere. Microstructure of the air cooled samples was revealed by polishing and etching in 2% nital solution and examined using optical microscopy.

Fracture stress was determined by four-point bending (4PB) testing in liquid nitrogen bath (-196°C) and at constant crosshead speed of 0.1mm/min, using notched Griffiths-Owen's specimens (12.7×12.7×80mm) with 45° notch angle and 0.25mm notch tip radius [8,9].

Scanning electron microscope (SEM) equipped with energy dispersive X-ray spectrometer (EDS) was used to examine fracture surface of the fractured 4PB specimens. Distance of the cleavage origin from the notch tip and cleavage facets diameters were measured from the SEM micrographs.

In order to provide data representing plastic response of the material at -196°C for FEM, uniaxial tension testing in liquid nitrogen bath was performed. Cylindrical specimens 5mm in diameter and with 30mm gauge length were used. The specimens were tested at constant crosshead speed of 0.1mm/min, giving the same order of magnitude of the true strain rate as in 4PB testing. True stress-true strain curve was plotted by non-linear polynomial regression of experimental data.

Elastic properties of the material were represented by Young's modulus of E=210GPa and Poisson's ratio of v=0.28.

Finite element analysis was performed using ABAQUS software package. Stress and strain distribution along the distance from the notch tip of the 4PB specimen was calculated using both 2D and 3D FEM, for the purpose of comparison.

Due to symmetry, only half of the 4PB specimen was modeled in the case of 2D FEM. Plane strain conditions were assumed. Quadratic eight node plane strain element type with full integration was used (CPEG8). Mesh was most refined along the notch tip, with minimal element size of $2 \cdot 10^{-3}$mm. Largest element size was 2mm. Total number of elements was 21746. 2D model assembly with meshed part is shown in Figure 1.

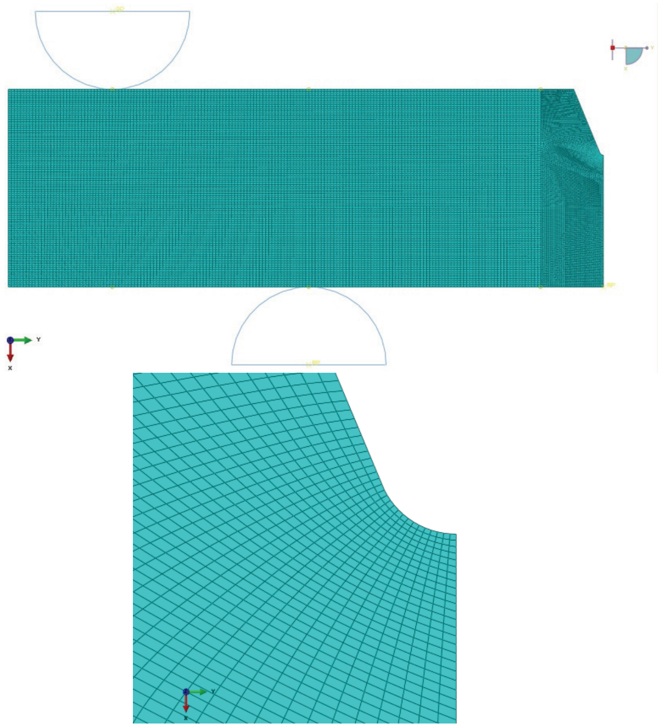

Fig. 1. 2D FEM assembly with the meshed 4PB specimen model part (left). Detail of the mesh at the notch tip of the specimen model (right).

In 3D FEM one quarter of the 4PB sample's geometry was analysed. Hexagonal eight-node element type with reduced integration was used (C3D8R). General element size was defined as 1. Mesh was gradually refined at the notch tip, down to 0.05 and at the supports to 0.2. Details of the meshed 3D model part are shown in Figure 2.

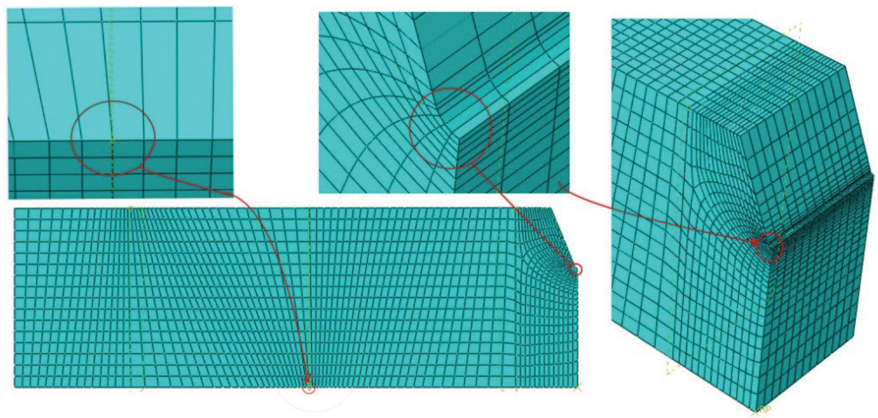

Fig. 2. Details of the 4PB specimen 3D model mesh.

In both models loading of the specimen was simulated by incremental translation of cylindrical supports that were defined as analytical rigid body in surface-to-surface contact with the part. Position of the supports is indicated in Figure 1.

Local cleavage fracture stresses and strains were determined from the stress and strain distribution curves at the distance of the cleavage origin from the notch tip. Effective surface energy was calculated according to Griffith's equation:

$$\sigma_F^* = \sqrt{\frac{\pi \cdot E \cdot \gamma}{(1-\nu^2)\cdot D}} \tag{1}$$

where σ_F^* is local cleavage fracture stress, γ – effective surface energy, D – diameter of the microcrack, E – modulus of elasticity and ν – Poisson's coefficient.

Results

Typical microstructure of the steel shown in Figure 3 consists of proeutectoid ferrite along former austenite grain boundaries, acicular ferrite that occupies most of the former austenite grain interiors and pearlite placed as a layer between them. An example of allotriomorphic ferrite with Widmanstätten ferrite side plates is also marked at micrograph, Figure 3.

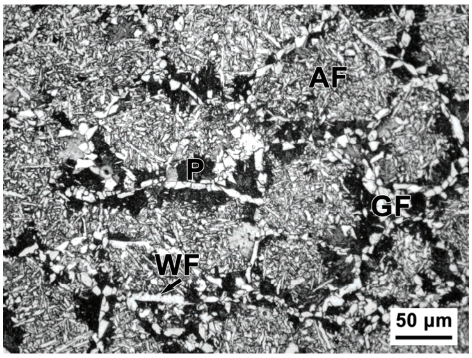

Fig. 3. Microstructure of the steel: AF–acicular ferrite, GF–grain boundary ferrite, P–pearlite and WF–Widmanstätten ferrite plates.

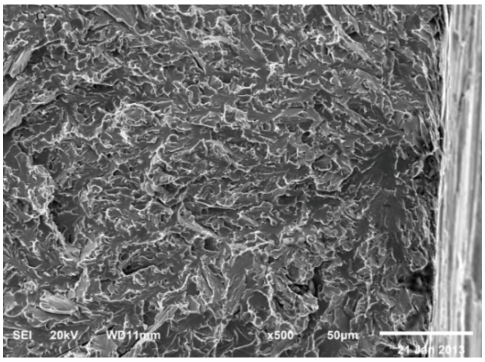

Fig. 4. SEM micrograph of the cleavage origin in 4PB specimen.

Fracture surface in the region of cleavage origin, near the notch tip is shown at SEM micrograph in Figure 4. Chevron shaped traces are clearly visible and could be easily traced back to the cleavage initiation site very close to the notch tip.

Stress and plastic strains distributions in front of the notch tip calculated by 2D and 3D FEM are compared in Figure 5.

Values of peak stresses, σ_{1max}, local cleavage fracture stress, σ_F^*, normalized by yield stress of the steel at -196°C, σ_0=775MPa, and critical plastic strains at the point of cleavage origin are summarized in Table 1, alongside with maximum bending load, F_{max}, and nominal bending stress at fracture, σ_{nom}. In addition, distance of the cleavage origin sites from the notch tip and effective diameters of the initial cleavage facet are given.

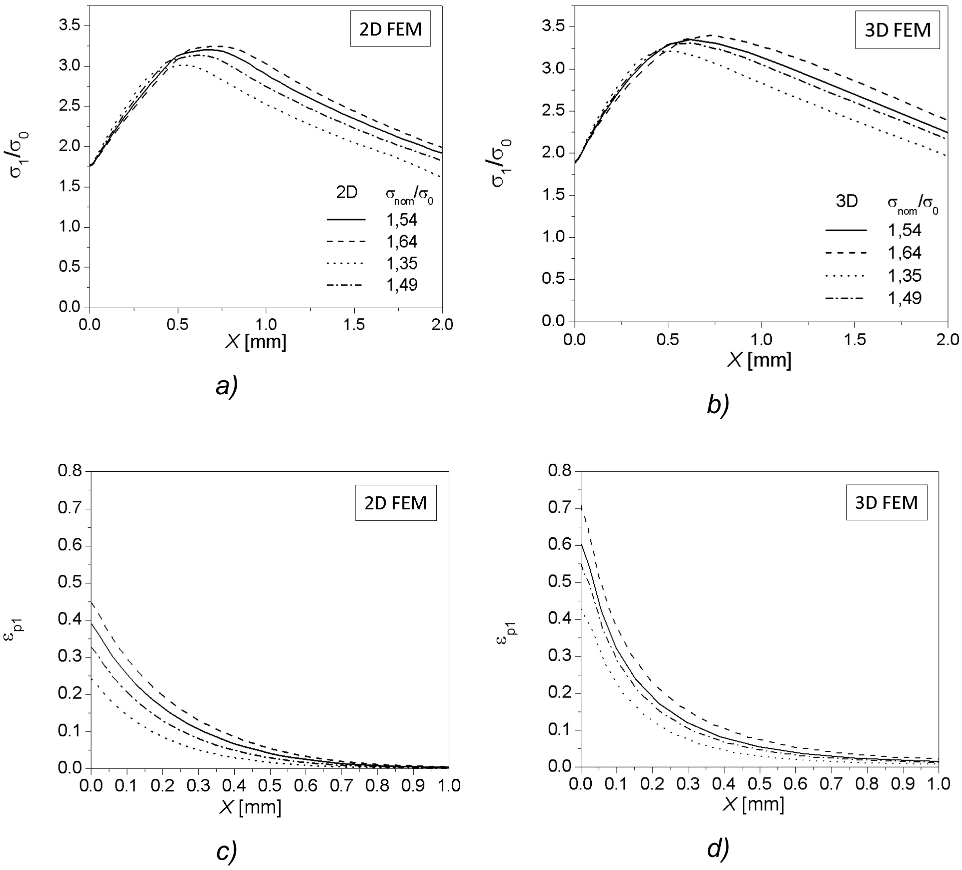

Fig. 5. Comparison of stress and strain distribution in front of the notch tip for 2D FEM (a, c) and 3D FEM (b, d).

Table 1. Fracture load, nominal fracture stress, peak stress and critical values of stress and plastic strain at the cleavage initiation site, determined by 2D and 3D FEM.

F_{max}. [KN]	σ_{nom} [MPa]	σ_{1max}/σ_0 2D	σ_{1max}/σ_0 3D	X_0 [μm]	D_{eff}	σ_F^*/σ_0 2D	σ_F^*/σ_0 3D	ε_{pc} 2D	ε_{pc} 3D
28.5	1193	3.20	3.35	41.1	11.3	1.87	2.02	0.331	0.480
30.3	1268	3.25	3.40	12.2	27.0	1.77	1.87	0.431	0.670
25.1	1049	3.01	3.21	19.4	18.8	1.81	1.94	0.219	0.394
27.6	1152	3.14	3.31	54.0	21.2	1.92	2.08	0.258	0.397

Discussion

The values of maximal principal stress along the distance from the notch tip are somewhat higher in the case of 3D FEM than for the 2D FEM for all tested samples (Figures 5a and 5b). Considering higher stress intensification in the case of plane strain conditions, higher stress in 2D FEM is to be expected [4].

The difference of the peak stress values between 2D and 3D model ranges from 4.7% to 6.5%, what is in good agreement with previously reported values of 3.5%–7.5% [4]. Such relatively small discrepancies could be ascribed to the variations in adopted FEM parameters, such as meshing the part, applied load history, number of increments, geometry nonlinearity etc. In that manner, smallest finite element in front notch tip in this paper is 50μm, in referent work of Wang and coworkers it is 70μm [4]. Total number of finite elements is significantly higher in present work. It should be kept in mind, however, that no other details of FEM used were revealed by the authors. Thus, a general conclusion is that the selection of modeling process could affect the value of calculated stresses. In addition, in both cases parabolic work hardening behavior of the material at -196°C was modeled. Thus, it could be concluded that the aforementioned discrepancies are not a consequence of the method that plastic response of the material was incorporated in the model.

Differences in critical fracture stresses, σ_F*, calculated using 2D and 3D FEM, given in Table 1, range from 5.6% to 9.0%. Those variations render the values of the effective surface energy, γ, of 49J/m^2 and 60J/m^2, for the 2D and 3D FEM respectively. Those values are in accordance to the results found in literature for medium carbon microalloyed steels [10,11]. It could be concluded that regarding stress calculation, plane strain 2D FEM could be considered as valid.

In the case of 3D model, lateral deformation takes place in narrow zone at the notch tip. The values of the true plastic strain in 3D FEM at the notch tip (distance X=0) are up to 0.71, while in the 2D FEM does not exceed 0.45 (Figures 5c and 5d). With increasing the distance from the notch tip, the plastic deformation is reduced as stress triaxiality increases and differences between values calculated by 2D and 3D FEM are diminishing.

The values of the critical plastic strain, ε_{pc}, i.e. plastic strain at the site of cleavage origin, determined using 3D FEM are by as much as 80% higher than for the 2D FEM. According to the results from the literature, in case of strain induced cleavage fracture, critical value of plastic strain, ε_{pc}, in circumstances of critical stresses triaxiality, should be considered in addition to the critical stress, σ_F* [4]. In that case, application of 2D FEM with plane strain assumption seems unjustified and plastic strains should be calculated using 3D FEM.

Conclusions

- Stresses in front of the notch tip calculated by 3D modeling are somewhat higher than for 2D FEM.
- The values of maximal principal strain in front of the notch tip calculated by 3D FEM are considerably higher than for 2D FEM. Therefore, in the case of strain induced cleavage fracture, it is necessary to determine the value of critical strain by using 3D FEM.

- The values of critical stress for cleavage fracture calculated by 2D FEM render the value of effective surface energy of 49J/m^2, whereas in the case of 3D FEM, this value is 60J/m^2.

Acknowledgements

The authors are indebted to Ministry of Education and Science of Serbia for financial support (Project OI174004) and Serbian Oil Company for supplying experimental material.

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