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Effect of hot-dip galvanization on the fatigue behaviour of welded structural steel

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Abstract

This paper investigates the effect of a galvanizing coating on the fatigue strength of S355 structural steel. While in the literature some results from fatigue tests made on unnotched specimens can be found, very few results are available dealing with notched components and, at the best of authors' knowledge, no results are available dealing with welded joints. The aim of the present paper is to partially fill this lack of knowledge. A comparison is carried out, between hot dip galvanized fillet welded cruciform joints made by S355 structural steel and not treated welded joints characterized by the same geometry, subjected to a load cycle $R = 0$. 34 new experimental data are summarized in the present contribution, in terms of stress range $\Delta\sigma$ and averaged strain energy density range $\Delta\overline{W}$ in a control volume of radius $R_0 = 0.28$ mm.

Keywords: galvanized steel; high cycle fatigue; fillet welded cruciform joint; SED.

1. Introduction

Hot-dip galvanizing is a surface treatment that allows protecting components from corrosion. Galvanizing is found in several industrial applications, in particular when iron or steel are used. Hot-dip galvanizing has a proven and growing history of success in a large number of applications worldwide.

While the monotonic behaviour of steel is not greatly affected by the presence of the zinc layer, except for the yield stress, under cyclic stress the fatigue strength is usually reduced. This point has been discussed by Bergengren and Melander (Bergengren and Melander, 1992) dealing with high-strength steels without any stress concentration effect or geometrical discontinuity. In (Bergengren and Melander, 1992) it was found that there is a reduction of the

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fatigue life increasing with the thickness of the zinc layer. On the other hand other authors did not find any correlation in terms of loss of the fatigue strength due to the coating thickness (Browne et al., 1975; Nilsson et al., 1989). The effect of a galvanizing coating on the fatigue strength of unnotched ferritic steel has been extensively investigated in (Vogt et al., 2001) and a tool based on the Kitagawa–Takahashi diagram has been employed for the prediction of the fatigue resistance of hot-dip galvanized steel. It was proven that the fatigue strength of a ferritic steel is not affected by the zinc layer if the thickness does not exceed the threshold value of 60 μm.

Some recent studies have been recently performed, dealing with galvanized steel wires for bridge construction (Jiang et al., 2009; Yang et al., 2012), the fatigue behaviour of two hot-dip galvanized steel with similar static load-bearing capability (Berchem and Hocking, 2007) and of galvanized rear axles made of micro-alloyed steel (Dimatteo et al., 2011). Other aspects tied to the galvanizing process are discussed in (Maaß and Peißker, 2011).

While in the literature some results from fatigue tests made on unnotched specimens are nowadays available, very few results are available dealing with notched components. At the best of authors' knowledge the only complete set of data from notched specimens is due to Huhn and Valtinat (Valtinat and Huhn, 2004). Low-cycle and high-cycle fatigue tests were carried out on S 235 JR G2 specimens. Plates with holes and bearing-type connections with punched and drilled holes were examined. Plates with holes were able to withstand a higher stress range $\Delta\sigma$ at the same number of cycles N up to failure than the joints. A comparison between specimens with punched holes and the ones with drilled holes has showed the negative influence of punching on the fatigue strength. However, a direct comparison between uncoated and hot-dip galvanized notched steel is not available in (Valtinat and Huhn, 2004) and it is not possible to quantify the fatigue strength reduction due to the galvanizing process. Finally, no results about the effect of hot-dip galvanization on the behaviour of welded structural steel are available. The main aim of the present paper is to partially fill this lack considering uncoated and hot-dip galvanized fillet welded cruciform joints made of structural steel S355. Two new fatigue sets of data are summarized in the present paper. The reduction of the fatigue strength due to the presence of the zinc layer is fully investigated. The results are shown in terms of stress range Δσ and of the averaged strain energy density range $\Delta\overline{W}$ in a control volume of radius $R_0 = 0.28$ mm

Nomenclature

2α	notch opening angle
γ	supplementary angle of α: $\gamma = \pi - \alpha$
ν	Poisson's ratio
$\Delta\sigma$	stress range
$\Delta\sigma_A$	fatigue strength in terms of stress range at N_A cycles
$\Delta K_{1,2,3}$	mode 1, 2 and 3 notch stress intensity factor range
ΔK_{1A}	fatigue strength in terms of notch stress intensity factor range at N_A cycles
$\Delta\overline{W}$	averaged strain energy density (SED)
$\Delta\overline{W}_C$	critical value of the SED range
$\lambda_{1,2,3}$	mode 1, 2 and 3 Williams' eigenvalues
E	Young's modulus
$e_{1,2,3}$	mode 1, 2 and 3 functions in the SED expression
f	frequency
$K_{1,2,3}$	mode 1, 2 and 3 notch stress intensity factor (NSIF)
k	inverse slope of the Wöhler curve
N	number of cycles
Ps	survival probability
R	load cycle ratio
R_0	radius of the control volume for the calculation of the averaged SED value
T_σ	scatter index referred to the stress range
T_W	scatter index referred to the SED range

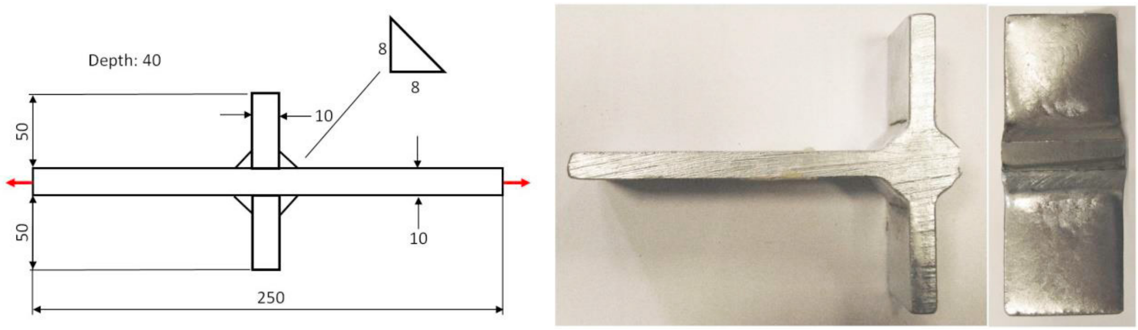

Fig. 1. Geometry of the fillet welded cruciform specimen and typical fracture surface.

2. Experimental details

The steel plates used to fabricate the samples were 10 mm in thickness, while the complete specimen had a global length of 250 mm. The complete geometry of the specimen can be seen in Fig. 1.

Fatigue tests have been conducted on transverse non-load carrying fillet welded joints, made of S 355J2+N structural steel. Welding beads have been made by means of automatic MAG (Metal Active Gas) technique. One of the two series of welded joints has been later hot dip galvanized.

Tests have been performed on a servo-hydraulic MTS 810 test system with a load cell capacity of 250 kN at 10 Hz frequency, in air, at room temperature.

All samples have been tested using a sinusoidal signal in uniaxial tension (plane loading) and a load ratio $R = 0$, under remote force control. Regarding the galvanized series, the coating treatment has been carried out at a bath temperature of 452°C and the immersion time was kept equal to 4 minutes for all the specimens. As a consequence, the coating thickness resulted in a range between 96 and 104 μm.

3. Results

Fatigue tests results are here presented in terms of the stress range $\Delta\sigma = \sigma_{max} - \sigma_{min}$ versus the number of cycles to failure, in a double logarithmic scale. The stress range is referred to the nominal area ($400\ \mathrm{mm}^2$).

Failure has always occurred at the weld toe, as expected, with a typical fracture surface as that shown in Fig. 1.

The results from the tests were statistically elaborated by using a log-normal distribution. The ‘run-out’ samples, over two million cycles, were not included in the statistical analysis and are marked in the graphs with an arrow.

Figure 2 refers to uncoated and coated series, while Figure 3 shows all the data elaborated together: in addition to the mean curve relative to a survival probability of $Ps = 50\%$, (Wöhler curve) the scatter band defined by lines with 10% and 90% of probability of survival (Haibach scatter band) is also plotted. The mean stress amplitude values corresponding to two million cycles, the inverse slope k value of the Wöhler curve and the scatter index T_σ (the ratio between the stress amplitudes corresponding to 10% and 90% of survival probability) are provided in the figure.

For the complete listing of the results of the fatigue tests, please refer to Table 1.

It can be noted, comparing the uncoated and coated series (Fig. 2), that the scatter index reduces from 1.6 to 1.3. This value is reasonably low both for the uncoated series and the galvanized one. Moreover also in terms of fatigue strength the effect of the galvanization is found to be negligible with a reduction, at $N = 2 \cdot 10^6$ and $Ps = 90\%$, from 83 to 82 MPa. Furthermore, from the data summarised in Fig. 3, it is possible to see that the fatigue strength at $N = 2 \cdot 10^6$ and $Ps = 90\%$ is 75 MPa: this value is comparable with the fatigue stress range (from 71 to 80 MPa) given for the corresponding detail category in Eurocode 3.

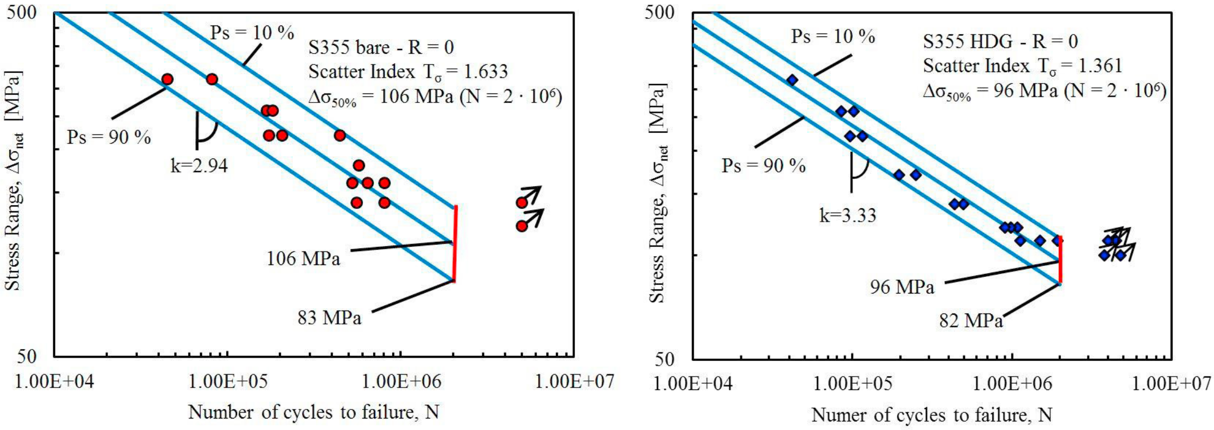

Fig. 2. Fatigue behaviour of bare (left) and galvanized (HDG, right) welded steel at $R = 0$.

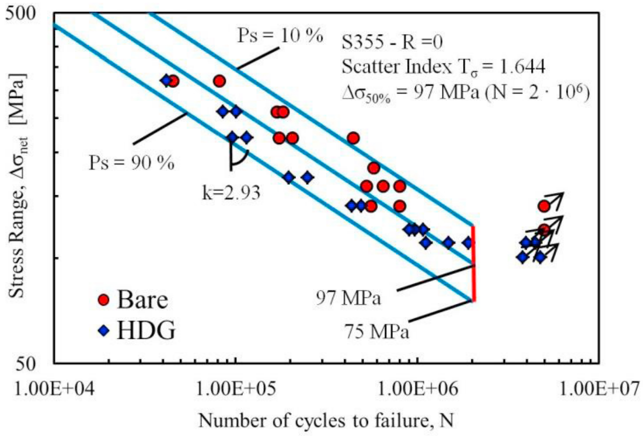

Fig. 3. Fatigue behaviour of both uncoated and galvanized welded steel at $R = 0$.

Table 1. Fatigue results from uncoated and coated (HDG) welded specimens.

UNCOATED SPECIMENS			
$\Delta\sigma$ [MPa]	N [cycles]		$\Delta\overline{W}$ [N mm/mm^3]
260	168750		0.5692
320	81500		0.8622
260	181484		0.5692
220	445750		0.4075
180	572333		0.2728
140	5000000	*Run out*	0.1650
160	803000		0.2155
160	523983		0.2155
140	804960		0.1650
140	556990		0.1650
160	645140		0.2155
320	45000		0.8622
120	5000000	*Run out*	0.1212
220	173000		0.4075
220	205616		0.4075

COATED SPECIMENS			
$\Delta\sigma$ [MPa]	N [cycles]		$\Delta\overline{W}$ [N mm/mm^3]
140	494000		0.1650
120	1079000		0.1212
100	4800000	*Run out*	0.0842
260	85000		0.5692
140	436500		0.1650
120	978200		0.1212
220	96820		0.4075
120	905500		0.1212
110	1125546		0.1019
100	3800000	*Run out*	0.0842
110	1500000		0.1019
110	4500000	*Run out*	0.1019
110	4000000	*Run out*	0.1019
260	101200		0.5692
170	195000		0.2433
170	250000		0.2433
110	1940000		0.1019
320	42000		0.8622
220	115000		0.4075

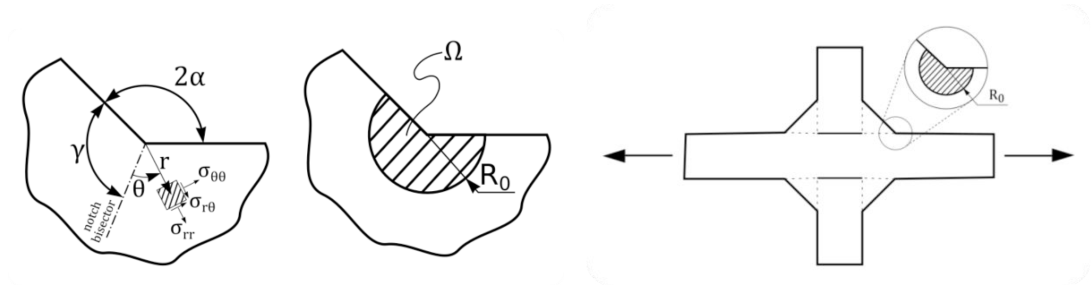

Figure 4. Polar coordinate system and critical volume (area) centered at the notch tip.

4. Strain Energy Density approach

An averaged strain energy density (SED) criterion has been proposed and formalized first by Lazzarin and Zambardi (Lazzarin and Zambardi, 2001), and later has been extensively studied and applied for static failures and fatigue life assessment of notched and welded components subjected to different loading conditions (Berto and Lazzarin, 2014). According to this volume-based criterion, the failure occurs when the mean value of the strain energy density $\overline{W}$ over a control volume with a well-defined radius R_0 is equal to a critical value W_C, which does not depend on the notch sharpness. The critical value and the radius of the control volume (which becomes an area in bi-dimensional problems) are dependent on the material (Berto and Lazzarin, 2014).

The SED approach was formalized and applied first to sharp, zero radius, V-notches (Lazzarin and Zambardi, 2001), considering bi-dimensional problems (plane stress or plane strain hypothesis). The volume over which the strain energy density is averaged is then a circular area Ω of radius R_0 centred at the notch tip, symmetric with respect to the notch bisector (Fig. 4), and the stress distributions are those by Williams (Williams, 1952), written according to Lazzarin and Tovo formulation (Lazzarin and Tovo, 1998). Dealing with sharp V-notches the strain energy density averaged over the area Ω turns out to be:

$$\overline{W} = \frac{e_1}{E}\left[\frac{K_1}{{R_0}^{1-\lambda_1}}\right]^2 + \frac{e_2}{E}\left[\frac{K_2}{{R_0}^{1-\lambda_2}}\right]^2 \tag{1}$$

Where E is the Young's modulus of the material, λ_1 and λ_2 are Williams' eigenvalues (Williams, 1952), e_1 and e_2 are two parameters dependent on the notch opening angle 2α and on the hypothesis of plane strain or plane stress considered. Those parameters are listed in Table 1 as a function of the notch opening angle 2α, for a value of the Poisson's ratio $\nu = 0.3$ and plane strain hypothesis. K_1 and K_2 are the Notch Stress Intensity Factors (NSIFs) according to Gross and Mendelson (Gross and Mendelson, 1972):

$$\begin{aligned} K_1 &= \sqrt{2\pi} \lim_{r\to 0} r^{(1-\lambda_1)} \left[\sigma_{\theta\theta}(r, \theta = 0)\right] \\ K_2 &= \sqrt{2\pi} \lim_{r\to 0} r^{(1-\lambda_2)} \left[\sigma_{r\theta}(r, \theta = 0)\right] \end{aligned} \tag{2}$$

The SED approach was then extended to blunt U- and V-notches (Lazzarin et al., 2009; Lazzarin and Berto, 2005), by means of the expressions obtained by Filippi et al. (Filippi et al., 2002) for the stress fields ahead of blunt notches, and to the case of multiaxial loading (Lazzarin et al., 2008), by adding the contribution of mode III.

Table 2. Values of the parameters in the SED expressions valid for a Poisson's ratio $\nu = 0.3$ (Beltrami hypothesis).

2α [rad]	γ [rad]	λ_1	λ_2	λ_3	e_1 Plane strain	e_2 Plane strain	e_3 Axis-sym.
0	π	0.5000	0.5000	0.5000	0.13449	0.34139	0.41380
π/6	11π/12	0.5014	0.5982	0.5455	0.14485	0.27297	0.37929
π/3	5π/6	0.5122	0.7309	0.6000	0.15038	0.21530	0.34484
π/2	3π/4	0.5445	0.9085	0.6667	0.14623	0.16793	0.31034
2π/3	2π/3	0.6157	1.1489	0.7500	0.12964	0.12922	0.27587
3π/4	5π/8	0.6736	1.3021	0.8000	0.11721	0.11250	0.25863

The SED approach has been successfully applied to the fatigue assessment of welded joints and steel V-notched specimens. Considering a planar model for the welded joints, the toe region was modelled as a sharp V-notch. A closed form relationship for the SED approach in the control volume can be employed accordingly to Eq. (1), written in terms of range of the parameters involved.

In the case of an opening angle greater than $102.6°$, as in transverse non-load carrying fillet welded joints (Fig. 4), only the mode I stress distribution is singular. Then the mode II contribution can be neglected, and the expression for the SED over a control area of radius R_0, centred at the weld toe, can be easily expressed as follows:

$$\Delta\overline{W} = \frac{e_1}{E}\left[\frac{\Delta K_1}{{R_0}^{1-\lambda_1}}\right]^2 \tag{3}$$

The material parameter R_0 can be estimated by equating the expression for the critical value of the mean SED range of a butt ground welded joints, $\Delta\overline{W}_C = \Delta\sigma_A/2E$, with the one obtained for a welded joint with an opening angle $2\alpha > 102.6°$. The final expression for R_0 is as follows (Lazzarin and Zambardi, 2001):

$$R_0 = \left(\frac{\sqrt{2e_1}\Delta K_{1A}}{\Delta\sigma_A}\right)^{\frac{1}{1-\lambda_1}} \tag{4}$$

In Eq. (4) ΔK_{1A} is the NSIF-based fatigue strength of welded joints ($211\ \text{MPa mm}^{0.326}$ at $N_A = 5\times10^6$ cycles with nominal load ratio $R = 0$) and $\Delta\sigma_A$ is the fatigue strength of the butt ground welded joint (155 MPa at $N_A = 5\times10^6$ cycles $R = 0$) (Livieri and Lazzarin, 2005). Introducing these values into Eq. (4), $R_0 = 0.28$ mm is obtained as the radius of the control volume at the weld toe for steel welded joints. For the weld root, modelled as a crack, a value of the radius $R_0 = 0.36$ mm has been obtained by (Livieri and Lazzarin, 2005), re-writing the SED expression for $2\alpha = 0$. Therefore it is possible to use a critical radius equal to 0.28 mm both for toe and root failures, as an engineering approximation (Livieri and Lazzarin, 2005). It is useful to underline that R_0 depends on the failure hypothesis considered: only the total strain energy density is here presented (Beltrami hypothesis), but one could also use the deviatoric strain energy density (von Mises hypothesis) (Lazzarin et al., 2003).

The SED approach was applied to a large bulk of experimental data: a final synthesis based on 900 fatigue data is shown in Fig. 5 (Berto and Lazzarin, 2014), including results from structural steel welded joints of complex geometries, for which fatigue failure occurs both from the weld toe or from the weld root. Also fatigue data obtained for very thin welded joints have been successfully summarized in terms of the SED (Lazzarin et al., 2013).

Recently, the SED approach has been extended to the fatigue assessment of notched specimens made of Ti-6Al-4V under multiaxial loading (Berto, Campagnolo, et al., 2015) and to high temperature fatigue data of different alloys (Berto, Gallo, et al., 2015; Gallo et al., 2015; Gallo and Berto, 2015). A new method to rapidly evaluate the SED value from the singular peak stress determined by means of numerical model has been presented by Meneghetti et al. (Meneghetti et al., 2015).

5. Results in terms of SED

FE analyses of the transverse non-load carrying fillet welded joint have been carried out applying as remote loads on the model the experimental values used for the fatigue tests. A control volume with a radius equal to 0.28 mm was realized in the model, in order to quantify the SED value in the control volume having the characteristic size for welded structural steel. The diagram of the SED range value $\Delta\overline{W}$ versus the number of cycles to failure N was plotted in a double logarithmic scale, summarizing the fatigue data for both bare and hot-dip galvanized specimens. With the aim to perform a direct comparison, the scatter band previously proposed for welded joints made of structural steel and based on more than 900 experimental data, Fig. 5, has been superimposed to the results of the present investigation (Fig. 6). For the detailed list of the SED values for both bare and HDG specimens corresponding to the stress ranges used in the fatigue tests, please refer to the last columns of Table 1.

It can be noted that hot-dip galvanized specimens have a lower fatigue strength than the bare specimens, but both bare and HDG data fall within the scatter band previously proposed in the literature for welded structural steel.

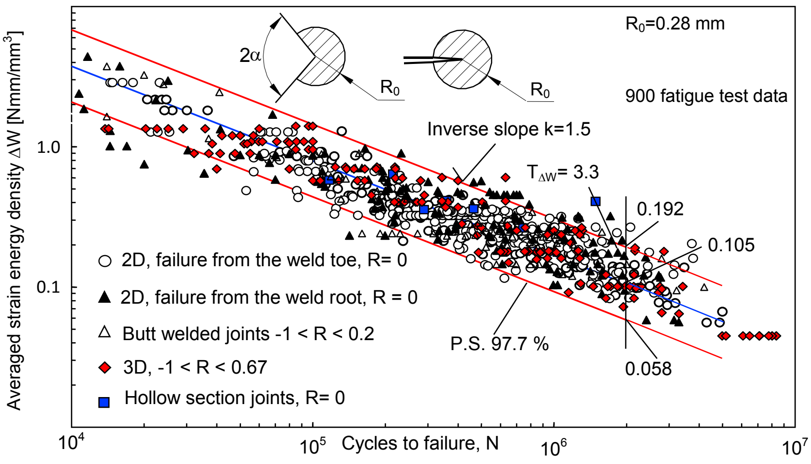

Fig. 5. Fatigue strength of welded joints made of structural steel as a function of the averaged local strain energy density.

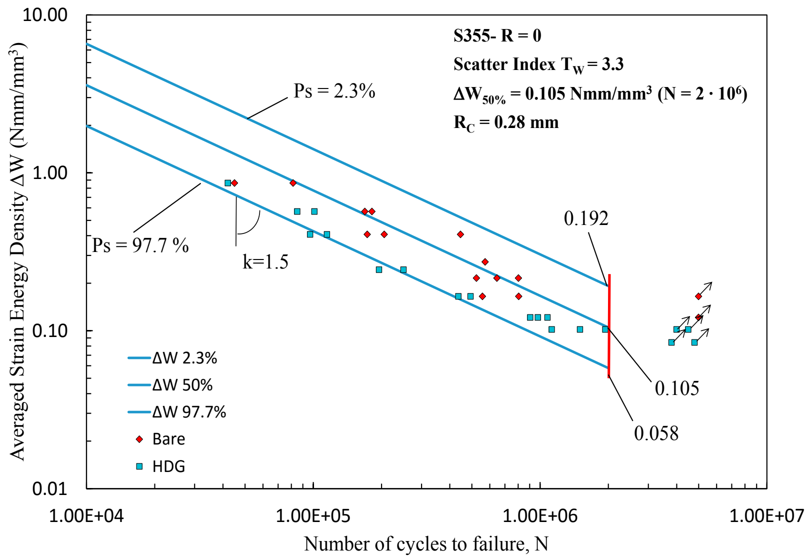

Fig. 6. Fatigue behaviour of uncoated and galvanized welded steel at $R = 0$ as a function of the averaged local strain energy density. Scatter band of 900 experimental data of welded joints made of structural steel is superimposed.

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