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ECONOMIC DESIGN OF WELDED I-BEAMS WITH PWT AND CELLULAR PLATES

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ABSTRACT

The cost optimization is important in structural design. Using various welding technologies the welding times and costs are different. The investigated structures are predominantly welded from plates. Examples are shown for design of welded I- beams and cellular plates. Fatigue fracture is one of the most dangerous phenomena for welded structures. In order to eliminate or decrease the danger of fatigue fracture several methods have been investigated. Post-welding treatments (PWTs) such as toe grinding, TIG-dressing, hammer peening and ultrasonic impact treatment (UIT) are the most efficient methods. The investigated cellular plates consist of two face sheets and some longitudinal ribs of square hollow section (SHS) welded between them using arc-spot welding technology. The cellular deck panels are subject to axial compression and a transverse load causing bending. In the optimization procedure the dimensions and number of longitudinal SHS ribs as well as the thickness of face sheets are sought which minimize the cost function and fulfil the design constraints. The design constraints relate to the stress due to compression and bending and to the eigenfrequency of the structure.

1. EFFECT OF POST-WELDING TREATMENTS ON THE OPTIMUM FATIGUE DESIGN OF WELDED I-BEAMS

Welding causes residual stresses and sharp stress concentrations around the weld, which are responsible for significant decrease of fatigue strength. Butt welds with partial penetration, toes and roots of fillet welds are points where fatigue cracks initiate and propagate. In order to eliminate or decrease the danger of fatigue fracture several post-welding treatments (PWTs) such as toe grinding, TIG-dressing, hammer peening and ultrasonic impact treatment (UIT) methods have been investigated. These methods have been tested and a lot of experimental results show their effectiveness and reliability. For designers it is important to know the measure of savings in structural weight and cost, which can be achieved by using these treatments. Optimum design is suitable for this task, since the additional cost of PWT can be included in the cost function and the improved fatigue stress range can be considered in the fatigue strength constraint. Thus, our aim is to illustrate this saving by means of a simple numerical example of a welded I-beam. In this case the transverse fillet welds used for vertical stiffeners decrease the fatigue stress range, thus the effect of PWT can be illustrated minimizing the cost function, which contains also the additional cost of PWT and the increased fatigue stress range can be included in the fatigue stress constraint.

1.1 IMPROVEMENT OF FATIGUE STRENGTH USING VARIOUS PWT-S

We have checked a number of articles, which either made measurements on different PWTs, or give data on the improvement in fatigue limit and about the speed of the process: Braid et al (1997), Woodley (1987), Janosch et al (1996), Huther et al (1996). For our purpose those publications are suitable, in which data are given not only for the measure of improvement (α), but also for the time required for treatment (T_0). These data are summarized in Table 1.

Table 1. Measure of improvement and specific treatment time for various treatments according to the published data

Method	Reference	T_0 (min/m)	Improvement %	α	Remark
Grinding	Woodley (1987)	60	40	1.4	
TIG dressing	Horn (1998)	18	40	1.4	can be 70-100%
Hammer peening	Braid (1997)	4	100	2.0	can be 175-190%
UIT	Janosch (1996)	15	70	1.7	

It should be mentioned that we want to calculate with the minimum value of improvement. A value larger than 100% cannot be realized in our numerical example.

1.2 MINIMUM COST DESIGN OF A WELDED I-BEAM CONSIDERING THE IMPROVED FATIGUE STRESS RANGE AND THE ADDITIONAL PWT COST

In the investigated numerical example transverse vertical stiffeners are welded to a welded I-beam with double fillet welds. PWT is used only in the middle of the span, since near supports the bending stresses are small. The tension part of stiffeners in the middle of span is not welded to the lower flange and to the lower part of the web. Thus, the PWT is needed only for welds connecting the stiffeners to the upper flange (Fig.1). For this reason two types of stiffeners are used as it can be seen in Fig.1.
The beam is loaded by a pair of forces fluctuating in the range of $0 - F_{max}$, so the bending stress range is calculated from F_{max}.

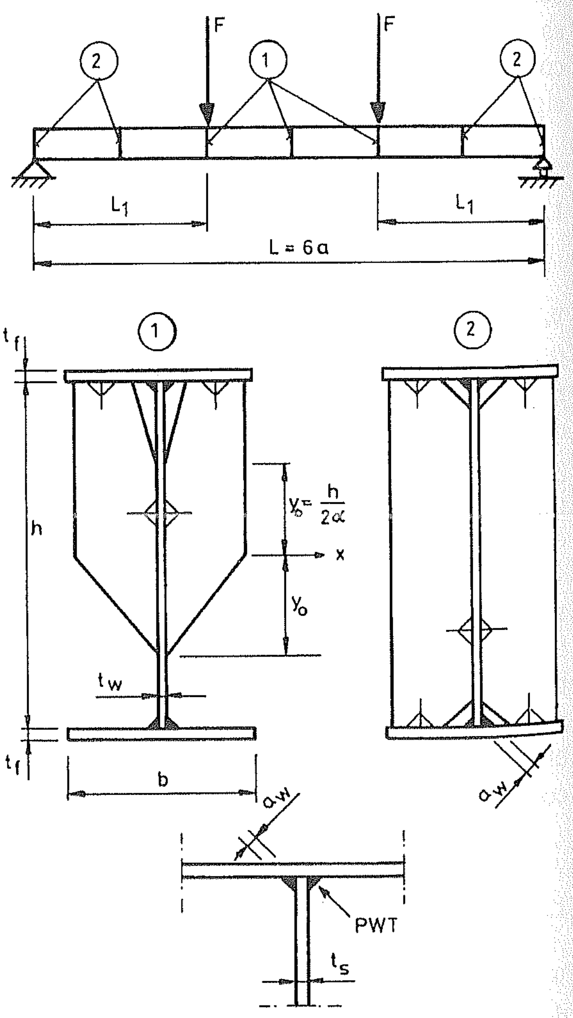

Fig. 1. Welded I-beam with vertical stiffeners. Double fillet welds with (1) and without (2) PWT

1.2.1 THE COST FUNCTION

In our previous studies we have used a cost function containing the material and fabrication costs as follows:

$$K = K_m + K_f = k_m \rho V + k_f \sum T_i \quad (1)$$

where ρ is the material density, V is the volume of the structure, k_m and k_f are the corresponding cost factors, T_i are the fabrication times.

Eq.(1) can be written in the form of

$$\frac{K}{k_m} = \rho V + \frac{k_f}{k_m} \sum T_i \tag{2}$$

We use the following cost factors: k_m = 0.5 - 1 \$/kg, $k_{f\,max}$= 60 \$/h = 1 \$/min, thus the ratio of k_f/k_m can be varied in a wide range of 0 - 2 kg/min. k_f/k_m = 0 means that K/k_m is a weight (mass) function, k_f/k_m = 2 kg/min can be used for developed countries.
The fabrication times can be calculated as follows:

$$\sum T_i = T_1 + T_2 + T_3 + T_4 \tag{3}$$

Time for preparation, assembly and tacking is

$$T_1 = C_1 \Theta_d \sqrt{\kappa \rho V} \tag{4}$$

where C_1 = 1 min/kg$^{0.5}$, Θ_d is a difficulty factor expressing the complexity of a structure (planar or spatial, consisting of plates or tubes etc.), κ is the number of elements to be assembled.
Time for welding is

$$T_2 = \sum C_{2i} a_{wi}^n L_{wi} \tag{5}$$

where $C_{2i} a_{wi}^n$ is given for different welding technologies and weld shapes according to COSTCOMP (1990) software and Jármai, Farkas (1999), a_w is the weld size, L_w is the weld length.
Time for additional works as deslagging, chipping and electrode changing is

$$T_3 = 0.3 T_2 \tag{6}$$

Time for PWT is

$$T_4 = T_0 L_t \tag{7}$$

T_0 is the specific time (min/mm), L_t is the treated weld length (mm).
The final form of the cost function is

$$\frac{K}{k_m} = \rho V + \frac{k_f}{k_m}\left(\Theta_d \sqrt{\kappa \rho V} + 1.3 \sum C_{2i} a_{wi}^n L_{wi} + T_0 L_t\right) \tag{8}$$

1.2.2 DESIGN CONSTRAINTS

The constraint on fatigue stress range can be formulated as

$$\frac{F_{\max} L_1}{W_x} \leq \frac{\alpha \Delta\sigma_C}{\gamma_{Mf}} \tag{9}$$

where $W_x = \dfrac{I_x}{\dfrac{h}{2} + \dfrac{t_f}{2}}$; $\qquad I_x = \dfrac{h^3 t_w}{12} + 2bt_f \left(\dfrac{h}{2} + \dfrac{t_f}{2}\right)^2$ (10)

According to Eurocode 3 (EC3) (1992) the fatigue stress range for as welded structure is $\Delta\sigma_C = 80$ MPa, the fatigue safety factor is $\gamma_{Mf} = 1.25$. α expresses the measure of improvement

$$\alpha = \frac{\Delta\sigma_{Cimproved}}{\Delta\sigma_{Caswelded}}.$$

The constraint on local buckling of the web according to EC3 is

$$\frac{h}{t_w} \leq 69\varepsilon; \qquad \varepsilon = \sqrt{\frac{235}{\alpha \Delta\sigma_C / \gamma_{Mf}}} \tag{11}$$

Note that we calculate in the denominator of ε with the maximum compressive stress instead of yield stress.
The constraint on local buckling of the compression flange is

$$\frac{b}{t_f} \le 28\varepsilon \tag{12}$$

1.2.3 NUMERICAL EXAMPLE

Data: F_{max} = 138 kN, L = 12 m, L_1 = 4 m, $\Delta\sigma_C / \gamma_{Mf} = 80/1.25 = 64$ MPa, $\varepsilon = 1.916/\sqrt{\alpha}$; $\Theta_d = 3$; number of stiffeners is $2x7 = 14$, thus $\kappa = 3 + 14 = 17$.
The volume of the structure is

$$V = (ht_w + 2bt_f)L + 4bht_S + 1.5bht_S\left(1 + \frac{1}{\alpha}\right); \qquad t_s = 6 \text{ mm} \tag{13}$$

The second member expresses the volume of stiffeners without PWT, the third member gives the volume of stiffeners with PWT.
For longitudinal GMAW-C (gas metal arc welding with CO_2) fillet welds of size 4 mm we calculate with

$$C_2 a_w^n L_w = 0.3394x10^{-3} x4^2 x4L = 260 \text{ min}, \tag{14}$$

for transverse SMAW (shielded metal arc welding) fillet welds the following formula holds

$$C_2 a_w^n L_w = 0.7889x10^{-3} x4^2 \left[6\left(b + \frac{2h}{\alpha}\right) + 16(b+h)\right] \tag{15}$$

For the constrained minimization of the nonlinear cost function the Rosenbrock's Hillclimb mathematical programming method is used complementing it with an additional search for optimum rounded discrete values of unknowns. The results of computation, i.e. the unknown dimensions h, t_w, b and t_f as well as the minimum costs for different values of k_f/k_m and α are given in Table 2.

Table 2. Optimum rounded dimensions in mm and K/k_m (kg) values for different k_f/k_m ratios for various PWT-s. $k_f/k_m = 0$ means the minimum weight design without effect of PWT

PWT	k_f/k_m (kg/min)	h	t_w	b	t_f	K/k_m (kg)
as welded	0	1300	10	320	14	2191
	1	1230	10	310	16	**3802**
	2	1230	10	310	16	**5399**
Grinding	1	940	9	340	15	3343
	2	890	8	300	19	4704
TIG dressing	1	1000	9	330	14	3235
	2	1110	10	310	12	4770
Hammer peening	1	820	9	310	13	**2762**
	2	820	9	310	13	**3999**
UIT	1	970	10	300	12	3021
	2	810	8	300	17	4202

It can be seen from Table 2. that with the various treatment methods the following cost savings can be achieved: grinding 14-15 %, TIG dressing 13–17 %, hammer peening 35-38 %, UIT 26-28 %.

Thus, the cost savings are significant the most efficient method is the hammer peening. It can be also seen, that PWT methods affect the optimum dimensions.

2 OPTIMUM DESIGN OF WELDED CELLULAR PLATES FOR SHIP DECK PANELS

Cellular plates consist of two face sheets and a grid of ribs welded between them. The main advantage of such plate structure is that the cells have a large torsional stiffness, which allows designers to construct plates of small height. The disadvantage of cellular plates lies in fabrication difficulty, since, when the height is smaller than 800-1000 mm, it is impossible to weld the ribs to the face sheets from inside.

Some applications of cellular plates are as follows: double bottoms of ships, rudders of ships, floating roofs of cylindrical storage tanks, box gates for dry docks, wings of aircraft structures, bridge decks, floating bridges, offshore platforms, elements of machine tool structures (press tables, mounting desks, base plates), mining shields, floors of buildings, lightweight roofs, etc.

Regarding the fabrication of cellular plates there are more possibilities to join the ribs to the face sheets. The simplest but not the cheapest solution is to use face plate elements and weld them to ribs from outside by fillet welds. Special welds such as arc-spot welds, slot or plug welds as well as electron-beam or laser welds can be used without cutting larger face sheet parts. Some applications combine the fillet and arc-spot welds.

The aim of present study is to work out a minimum cost design of such cellular plates considering, besides of stress constraint, the eigenfrequency constraint as well, and the fabrication cost of arc-spot welds. We applied square hollow section (SHS) instead of rectangular hollow section (RHS) to minimise the height of the panel. The main specialities of this application are as follows: 1) only longitudinal ribs of square hollow section (SHS) are used joined to the face sheets by arc-spot welding, thus, in the cost function the fabrication cost of arc-spot welds should be included; 2) to avoid the vibration resonance, the first eigenfrequency of the plate should be larger than a prescribed value.

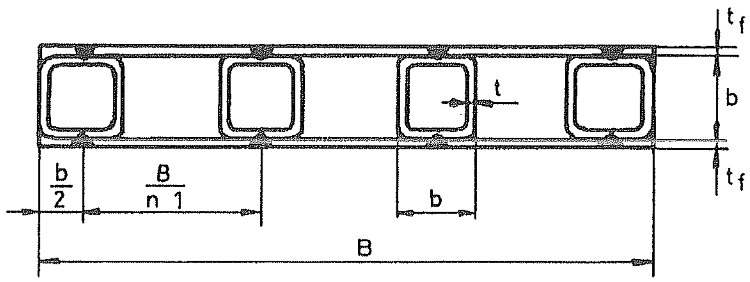

Fig.2. Cross-section of the ship deck panel investigated in longitudinal stiffeners

2.1 THE COST FUNCTION

The cross-section of the deck panel is shown in Fig.2. The cellular plate consists of two face sheets of thickness t_f and longitudinal SHS ribs of number n with dimensions of b and t.

In the longitudinal direction the plate ends are clamped and the panel is supported in two places, thus, it can be calculated as a three-span beam (Fig.3) loaded axially with a compression stress

$\sigma = N / A_{eff}$, A_{eff} being the effective cross-section for compression (Fig.5), and transversely by a uniformly distributed normal load of a factored intensity p.

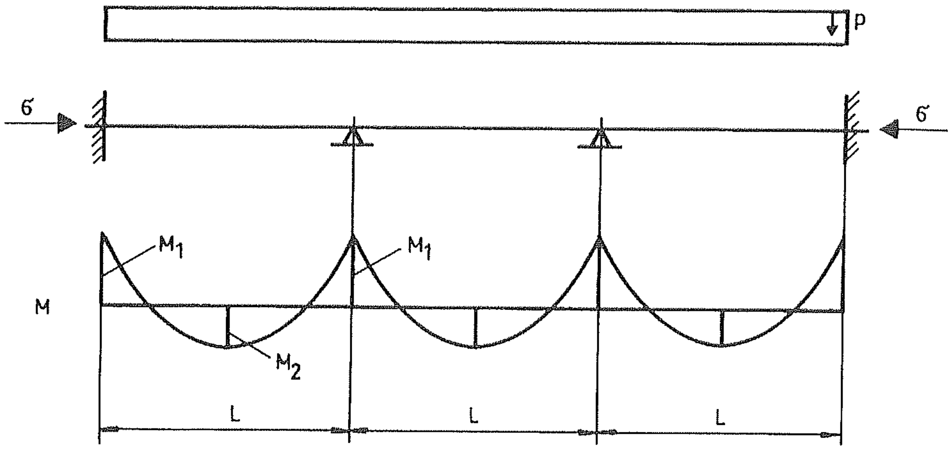

Fig.3. Bending moment diagram of the ship deck panel

The cost of a welded structure consists of material and fabrication cost according to Eq.(2).
The volume of the structure is

$$V = 3L(nA_{SHS} + 2Bt_f) \tag{16}$$

The cross-sectional area of a SHS is, considering the corner roundings according to a formula given by DASt (1986), approximately

$$A_{SHS} = 0.99 * 4(b-t)t\left(1 - 0.43\frac{t}{b-3t}\right) \tag{17}$$

At the fabrication time (Eq. 4) κ is the number of assembled structural elements, in our case it is $\kappa = n + 2$.
The time of arc-spot welding is given by

$$T_2 = n_S T_S \tag{18}$$

where n_S is the number of spots, T_S is the time of welding of one spot weld and of the electrode transfer to the next spot.
The additional time for deslagging, chipping and changing the electrode can be calculated as Eq.(6).

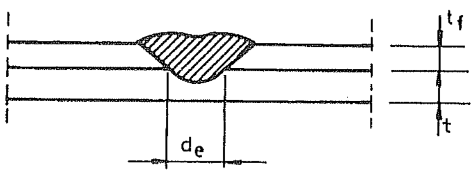

Fig.4. Effective diameter of an arc-spot weld

Since data for T_S cannot be found in literature, we take T_S = 0.3 min noting that it depends on the welding equipment and the degree of automation. The number of spots can be calculated by means of the spot pitch a.
The required minimum spot pitch can be determined considering a spot weld as a pin (Blodgett (1978), Füchsel et al. (1990)).

2.2 THE DESIGN CONSTRAINTS

Constraint on eigenfrequency

A serviceability constraint can be defined expressing that the first eigenfrequency of a simply supported bent beam of span length L should be larger than a prescribed value

$$f_1(Hz) = \frac{\pi}{2L^2}\left(\frac{10^3 EI_x}{m}\right)^{1/2} \geq f_0 \quad (19)$$

where E is the modulus of elasticity, the moment of inertia of the whole cross-section is

$$I_x = nI_{SHS} + Bt_f(b+t_f)^2/2 \quad (20)$$

According to DASt (1986) the moment of inertia of a SHS is approximately

$$I_{SHS} = \frac{2}{3}(b-t)^3 t\left(1 - 0.86\frac{t}{b-3t}\right) \quad (21)$$

In the mass m an additive mass m_{add} should be considered, thus

$$m = \rho\left(nA_{SHS} + 2Bt_f\right) + m_{add} \quad (22)$$

It should be mentioned that f_1 is in reality larger than it is calculated with formula (19) because the beam is clamped and not simply supported, but this approximation can be used since this constraint is not active.

Constraint on stability due to compression and bending

According to EC3, the stress constraint should be defined for a section of class 4 as follows:

$$\frac{N}{\chi A_{eff} f_{y1}} + \frac{k_x \psi M_1}{W_\xi f_{y1}} \leq 1 \qquad f_{y1} = f_y/\gamma_{M1};\ \gamma_{M1} = 1.1 \quad (23)$$

where χ is the overall buckling factor

$$\chi = \frac{1}{\phi + \left(\phi^2 - \bar{\lambda}^2\right)^{1/2}} \quad (24)$$

$$\phi = 0.5\left[1 + 0.34\left(\bar{\lambda} - 0.2\right) + \bar{\lambda}^2\right] \quad (25)$$

$$\bar{\lambda} = \frac{KL}{\lambda_1 r}\beta_A^{1/2} \quad (26)$$

for a beam with clamped ends $K = 0.5$,

$$\lambda_1 = \pi\left(E/f_y\right)^{1/2}\beta_A^{1/2}; \qquad r = \left(I_{eff}/A_{eff}\right)^{1/2} \quad (27)$$

$$\beta_A = \frac{A_{eff}}{nA_{SHS} + 2Bt_f} \quad (28)$$

To obtain the effective cross-section, the effective width of face sheets should be calculated according to EC3

$$b_e = \rho_p \frac{B}{n-1} \quad (29)$$

$$\bar{\lambda}_p = \frac{B/\left[(n-1)t_f\right]}{28.4\varepsilon k_\sigma^{1/2}}, \qquad \varepsilon = \left(235/f_y\right)^{1/2} \quad (30)$$

with $k_\sigma = 4$ $\qquad \bar{\lambda}_p = \dfrac{B}{56.8\varepsilon(n-1)t_f}$ (31)

when $\bar{\lambda}_p \leq 0.673$ $\qquad \rho_p = 1$ (32a)

when $\bar{\lambda}_p \geq 0.673$ $\qquad \rho_p = \dfrac{1}{\bar{\lambda}_p} - \dfrac{0.22}{\bar{\lambda}_p^2}$ (32b)

Considering the effective cross-section shown in Fig.5 we get

$$A_{eff} = nA_{SHS} + 2B_e t_f \ , \qquad B_e = b + (n-1)b_e \tag{33}$$

and $\quad I_{eff} = nI_{SHS} + B_e t_f \left(b + t_f\right)^2 / 2$ (34)

According to the moment diagram shown in Fig.3

$$M_1 = BpL^2 / 12 \tag{35}$$

this bending moment should by multiplied by a dynamic factor ψ .

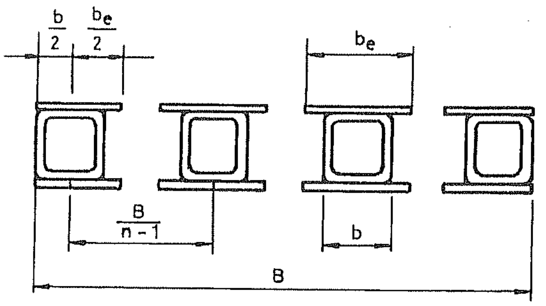

Fig.5. Effective cross-section for compression

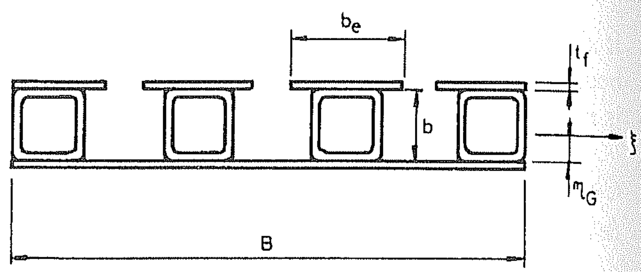

Fig.6. Effective cross-section for bending

$$k_x = 1 - \frac{\mu_x N}{\chi\left(nA_{SHS} + 2Bt_f\right)f_y} \qquad \text{but} \quad k_x \leq 1.5 \tag{36}$$

$$\mu_x = \bar{\lambda}(2\beta_M - 4) \qquad \text{but} \quad \mu_x \leq 0.9 \tag{37}$$

For our case it is $\beta_M = 1.3$ and $\mu_x = -1.4\bar{\lambda}$, thus

$$k_x = 1 + \frac{1.4\bar{\lambda}\beta_A N}{\chi f_y A_{eff}} \tag{38}$$

For bending another asymmetric effective cross-section should be taken into account as shown in Fig.6. The distance of gravity centre G is

$$\eta_G = \frac{nA_{SHS}\left(b + t_f\right)/2 + B_e t_f \left(b + t_f\right)}{nA_{SHS} + B - 2nt_f + B_e t_f} \tag{39}$$

The deduction of holes caused by arc-spot welds is considered for face sheet subject to tension by taking $B - 2nt_f$ instead of B.

The moment of inertia is given by

$$I_\xi = nI_{SHS} + nA_{SHS}\left(\frac{b + t_f}{2} - \eta_G\right)^2 + (B - 2nt_f)t_f\eta_G + B_e t_f \left(b + t_f - \eta_G\right)^2 \tag{40a}$$

the static moment is

$$S_\xi = b_e\left(b + t_f - \eta_G\right) \tag{40b}$$

and the section modulus is

$$W_\xi = \frac{I_\xi}{b + t_f - \eta_G} \tag{41}$$

Stress constraint for the upper face sheet

The upper face sheet is subject to static bending and compression in longitudinal direction as well as to bending in transverse direction. The stress due to longitudinal compression and bending is

$$\sigma_L = \frac{N}{A_{eff}} + \frac{\psi M_1}{W_\xi} \tag{42}$$

and the stress due to transverse bending, considering a plate strip with clamped edges of span length $B/(n-1) - b$, is

$$\sigma_T = \frac{\psi p}{2t_f^2}\left(\frac{B}{n-1} - b\right)^2 \tag{43}$$

The stress constraint can be expressed as

$$\left(\sigma_L^2 + \sigma_T^2 + \sigma_L \sigma_T\right)^{0.5} \le f_{y1} \tag{44}$$

2.3 THE OPTIMIZATION PROCEDURE

In the minimum cost design the optimum values of b, t, t_f and n are sought, which minimize the cost function (2) and fulfil the design constraints (19), (23) and (44). In the first phase the above mentioned variables are treated as continuous ones and the optima are determined using the Rosenbrock's Hillclimb mathematical programming method. In the second phase the discrete values of variables are calculated using a complementary search method. In this search the minimum values are taken as $_{\min} = 30$, $t_{\min} = 2$, $t_{f\min} = 2$ mm and $n_{\min} = 4$. The discrete values of SHS are sought according to the pre-standard prEN 10219-2 (1992).

Note that the minimum number of ribs $n_{\min} = 4$ has been selected, since the transverse stiffness of the panel is in the case of $n = 3$ too small. In the case when the normal loading is not uniformly distributed in transverse direction, it would be necessary to use transverse ribs as well to avoid too large torsional deformations.

The numerical data are as follows: $f_0 = 18$ Hz, $E = 2.1*10^5$ MPa, $B = 2000$, $L = 2250$ mm, $\Theta_d = 3$, $\rho = 7850$ kg/m^3 = $7.85*10^{-6}$ kg/mm^3, $m_{add} = 2*50 = 100$ kg/m = 0.1

Table 3. Optimization results for $f_y = 235$ MPa: number of ribs n, optimum dimensions in mm and K/k_m-values in kg for cost in function of the ratio k_f/k_m

k_f/k_m	n	t_f	b	t	K/k_m
	4	8	120	3	1987
	5	4	120	3	1212
	6	4	30	2	916
0	7	2.5	40	2	639
	8	2	50	2	**582**
	9	2	50	2	602
	10	2	50	2	621
	4	8	120	3	2367
	5	4	120	3	1620
	6	4	30	2	1331
1	7	2.5	40	2	**1161**
	8	2	50	2	1232
	9	2.5	40	2	1306
	4	8	120	3	2747
	5	4	120	3	2027
	6	4	30	2	1745
2	7	2.5	40	2	**1683**
	8	2.5	40	2	1813
	9	2.5	40	2	1943

kg/mm, $p = 3.5$ kN/m^2 = 3.5*10^{-3} N/mm^2, $\psi = 1.4$, $\sigma = N / A_{eff} = 150$ MPa.

The computational results are summarized in Table 3.

The optimum number of ribs is larger for minimum weight design ($k_f/k_m = 0$) i.e. $n = 8$ for $f_y = 235$ and $n = 6$ for $f_y = 355$ MPa, than for minimum cost design ($k_f/k_m = 1$ or 2) i.e. $n = 7$ for $f_y = 235$ and $n = 6$ or 5 for $f_y = 355$ MPa. The optimum number of ribs depends on f_y. Cost savings of 14-18% can be achieved using steel of yield stress 355 instead of 235 MPa.

The cost difference between the best and worst solution for $f_y = 235$ MPa and $k_f/k_m = 2$ is 100(2747-1683)/1683 = 63%, which emphasizes the importance of structural optimization. Calculations show that the stability and stress constraints are in most cases active and the eigenfrequency constraint is passive.

Acknowledgements

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References

Braid,J.E.M., Bell,R., Militaru,D.V.(1997): Fatigue life of as-welded, repaired, and hammer-peened joints in high-strength structural steel. Welding in the World, **39** (5), 248-261.

Blodgett,O.W.(1978): Report on proposed standards for sheet steel structural welding. Weld.J. **57**, 15-24.

COSTCOMP(1990): Programm zur Berechnung der Schweisskosten. Düsseldorf, Deutscher Verlag für Schweisstechnik.

DASt (Deutscher Ausschuss für Stahlbau) (1986): Richtlinie 016. Bemessung und konstruktive Gestaltung von Tragwerken aus dünnwandigen kaltgeformten Bauteilen. Köln.

Eurocode 3.(1992): Design of steel structures. Part 1.1. Brussels, CEN European Committee for Standardization.

Farkas,J., Jármai,K.(1997): Analysis and optimum design of metal structures. Balkema, Rotterdam-Brookfield.

Füchsel,S., Möbius,W. and Steinert,G.(1990): Empfehlung zur Berechnung von MAG-Punktschweissverbindungen. ZIS-Report, Halle, **1**, 31-36.

Horn,A.A., Huther,I., Lieurade,H.P.(1998): Fatigue behaviour of T-joints improved by TIG dressing. Welding in the World, **41** (4), 273-280.

Huther,I., Lieurade,H.P et al.(1996): Analysis of results on improved welded joints. Welding in the World **37** (5), 242-266.

Janosch,J.J., Koneczny,H. et al.(1996): Improvement of fatigue strength in welded joints (in HSS and aluminium alloys) by ultrasonic hammer peening. Welding in the World, **37** (2), 72-83.

Jármai,K., Farkas,J.(1999): Cost calculation and optimization of welded steel structures. Journal of Constructional Steel Research, **50,** 115-135.

prEN 10219-2 (1992): Cold formed structural hollow sections of non-alloy and fine grain structural steels. Part 2. Tolerances, dimensions and sectional properties. European Committee for Standardization, Brussels. German version DIN EN 10219 Teil 2. Entwurf. 1993.

Woodley,C.C.(1987): Practical applications of weld toe grinding. In Improving the Fatigue Strength of Welded Joints. The Welding Institute, Abington, Cambridge, UK, 1983. 19-22.