Stress Analysis of Failed Steam Generator Manway Studs from Oconee Unit 3 of Duke Power Company

Attachment 1 to Report No. IE-123

April 17, 1981

Prepared for: United States Nuclear Regulatory Commission
Office of Inspection and Enforcement

NRC Contract 05-80-251
PAR: NRC-IE-80/81 Task 03

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INTRODUCTION

STEAM GENERATOR MANWAY STUDS HAVE CRACKED IN THE PRIMARY SYSTEM AT OCONEE UNIT 3 OF DUKE POWER COMPANY.

METALLURGICAL AND PHYSICAL PROPERTIES OF THESE STUDS HAVE BEEN EXAMINED AND CORROSION PRODUCTS HAVE BEEN ANALYZED AS PART OF THE EFFORT TO IDENTIFY THE MODE OF FAILURE.

THE FINAL PART OF THE EFFORT TO IDENTIFY THE MODE OF FAILURE IS THE FRACTURE MECHANICS ANALYSIS CONTAINED IN ATTACHMENT 2 OF REPORT NO. IE-123. THE STUD LOAD CALCULATED IN THIS ATTACHMENT IS USED IN THE FRACTURE MECHANICS ANALYSIS.

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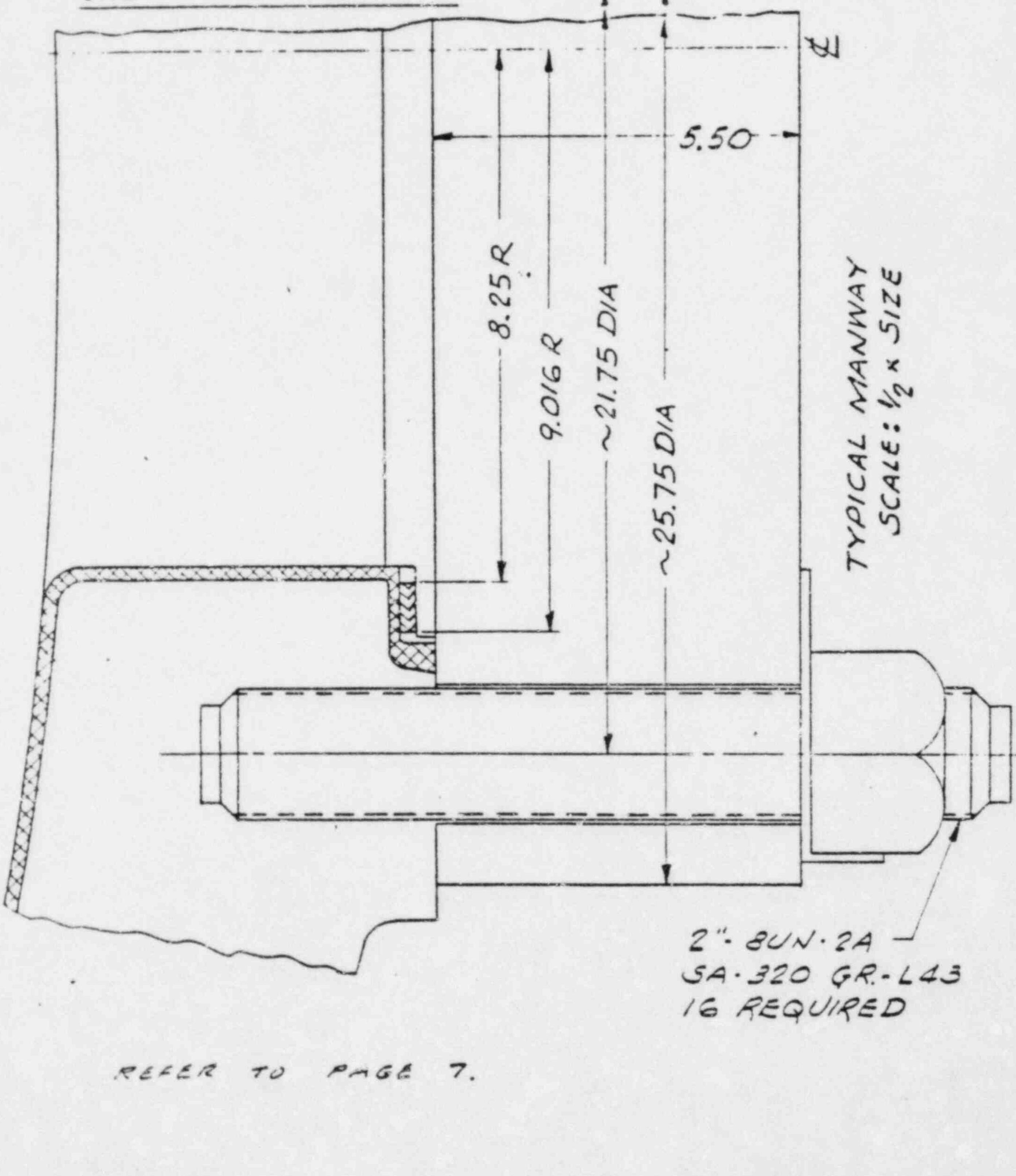
SKETCH OF MANWAY



REFER TO PAGE 7.

SUMMARY OF RESULTS

1. AS REPORTED ON PAGE 9, DIRECT TENSILE PRELOAD STRESS OF THE MANWAY STUDS IS 31820 PSI WITHOUT STRESS CONCENTRATION FACTOR (SCF) AND 95460 PSI WITH SCF.

   THESE DIRECT TENSILE STRESSES DO NOT INCLUDE BENDING STRESSES, WHICH SHOULD BE NEGLIGIBLE BECAUSE THE MANWAY COVER HAS FULL CONTACT WITH THE MANWAY FLANGE.

2. AS REPORTED ON PAGE 13, DIRECT TENSILE STRESS OF THE MANWAY STUDS CAUSED BY PRELOAD PLUS OPERATING PRESSURE IS 33718 PSI WITHOUT SCF AND 101,154 PSI WITH SCF.

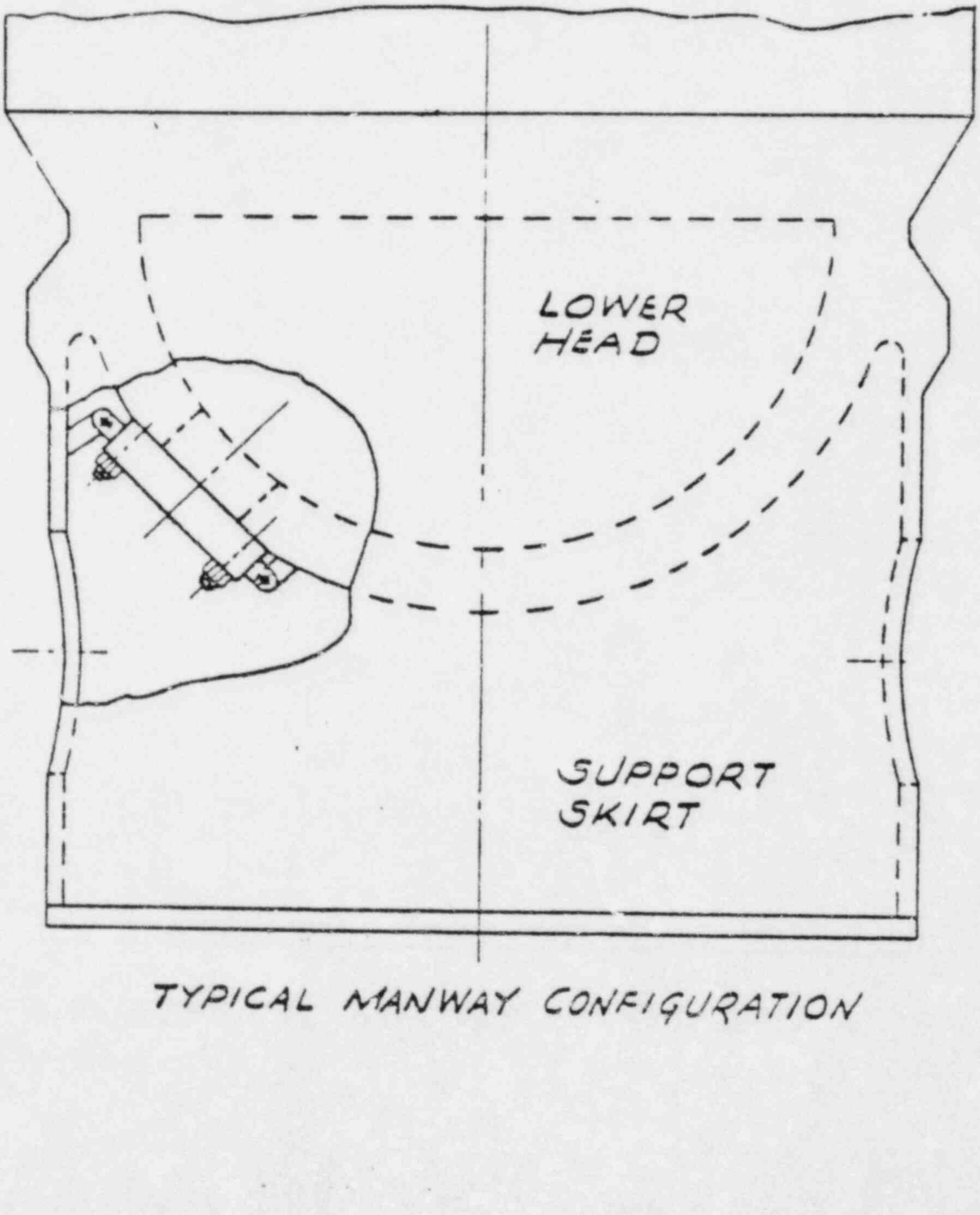
   THE FRACTURE MECHANICS ANALYSIS IN ATTACHMENT 2 IS BASED ON THIS CONDITION, WITH LOAD OF 93400 LB PER STUD.

3. AS CHECKED ON PAGE 14, THE MANWAY STUDS ARE SIZED CORRECTLY FOR THE CONDITION OF 2500 PSIG DESIGN PRESSURE ONLY.

   THE DIRECT TENSILE STRESS OF 13198 PSI IS LESS THAN THE ALLOWABLE OF 25000 PSI FOR SA-320 GRADE L 43 MATERIAL.

SKETCH OF LOWER HEAD
AND SUPPORT SKIRT
OF STEAM GENERATOR

REFER TO PAGE 7.



TYPICAL MANWAY CONFIGURATION

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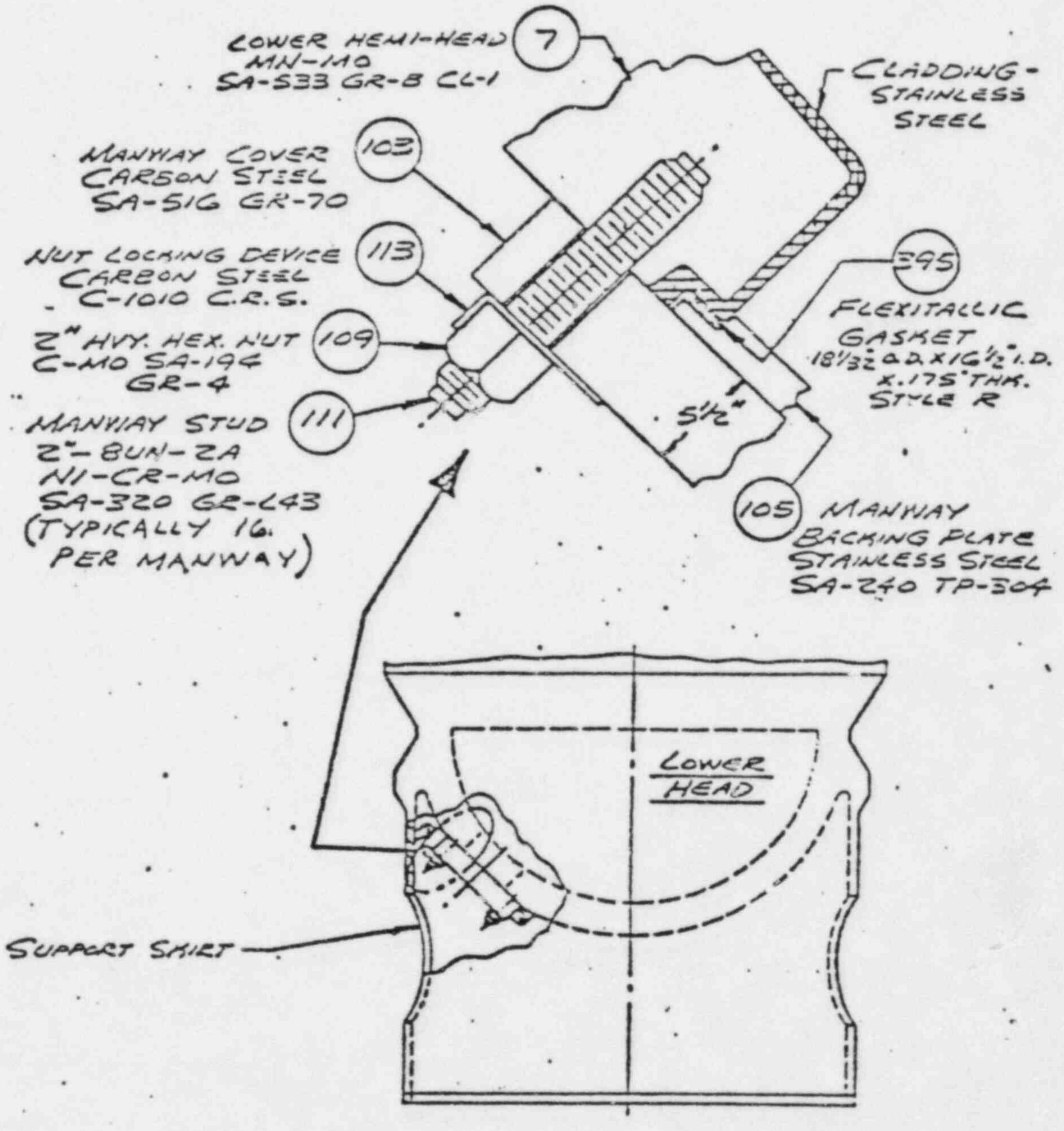
OF REPORT IE-123



TYPICAL MANWAY CONFIGURATION

RECEIVED WITH REF [6]
BY PARAMETER, INC.

PRELOAD STRESS OF STUD

CALCULATING PRELOAD STRESS
PER REF [3] PAGE 342,

d = NOMINAL DIAMETER OF STUD
= 2.00 IN.

S_{b_i} = PRELOAD STRESS OF STUD
$= 45,000 \sqrt{d}$
= 31820 PSI

CALCULATING PRELOAD STRESS
PER REF [2] PAGE 159,

T = APPLIED TORQUE
= 1950 × 12
= 23400 IN.-LB

C = TORQUE COEFFICIENT
= 0.15 , LUBRICATED

D = NOMINAL BOLT SIZE
= 2.00 IN.

F_i' = INITIAL TENSION PER STUD
$= T/CD = 23400/0.15 \times 2.00$
= 78000 LB

A = TENSILE STRESS AREA
OF 2.000 - 8 UN - 2A STUD
$= 2.77\ IN.^2$

S_{b_i} = PRELOAD STRESS OF STUD
$= F_i'/A = 78000/2.77$
= 28160 PSI

PRELOAD STRESS OF STUD, CONT

NOTING THAT CORRELATION BETWEEN THE TWO METHODS OF CALCULATING PRELOAD STRESS IS QUITE GOOD, LET US USE THE HIGHER VALUE.

THEN, S_{b_i} = 31820 PSI, WITHOUT STRESS CONCENTRATION

IN ORDER TO TAKE STRESS CONCENTRATION INTO ACCOUNT, LET US MAKE USE OF REF [4] PAGE 112.

NOTE THAT HETÉNYI'S RESULTS APPLY TO WHITWORTH THREADS AT STUD-TO-NUT CONNECTIONS. IN THE THREADED LENGTH OF STUD NOT IN CONTACT WITH THE NUT THE STRESS CONCENTRATION FACTOR WOULD BE LESS THAN THOSE REPORTED BY HETÉNYI.

ANOTHER FACTOR TO CONSIDER IN SELECTING A REASONABLE VALUE OF STRESS CONCENTRATION FACTOR IS THAT THE THREAD ROOTS OF WHITWORTH THREADS ARE FULLY ROUNDED, IN CONTRAST TO THE FLAT ROOTS WHICH ARE ALLOWABLE FOR UN THREADS. FOR A COMPARISON OF WHITWORTH AND UN THREADS, REFER TO THE FOLLOWING PAGE.

IN VIEW OF THE FOREGOING CONSIDERATIONS, IT APPEARS REASONABLE TO APPLY A STRESS CONCENTRATION FACTOR OF 3.0.

THEN, $K_t S_{b_i} = 3.0 \times 31820$

$= 95460$ PSI, WITH STRESS CONCENTRATION

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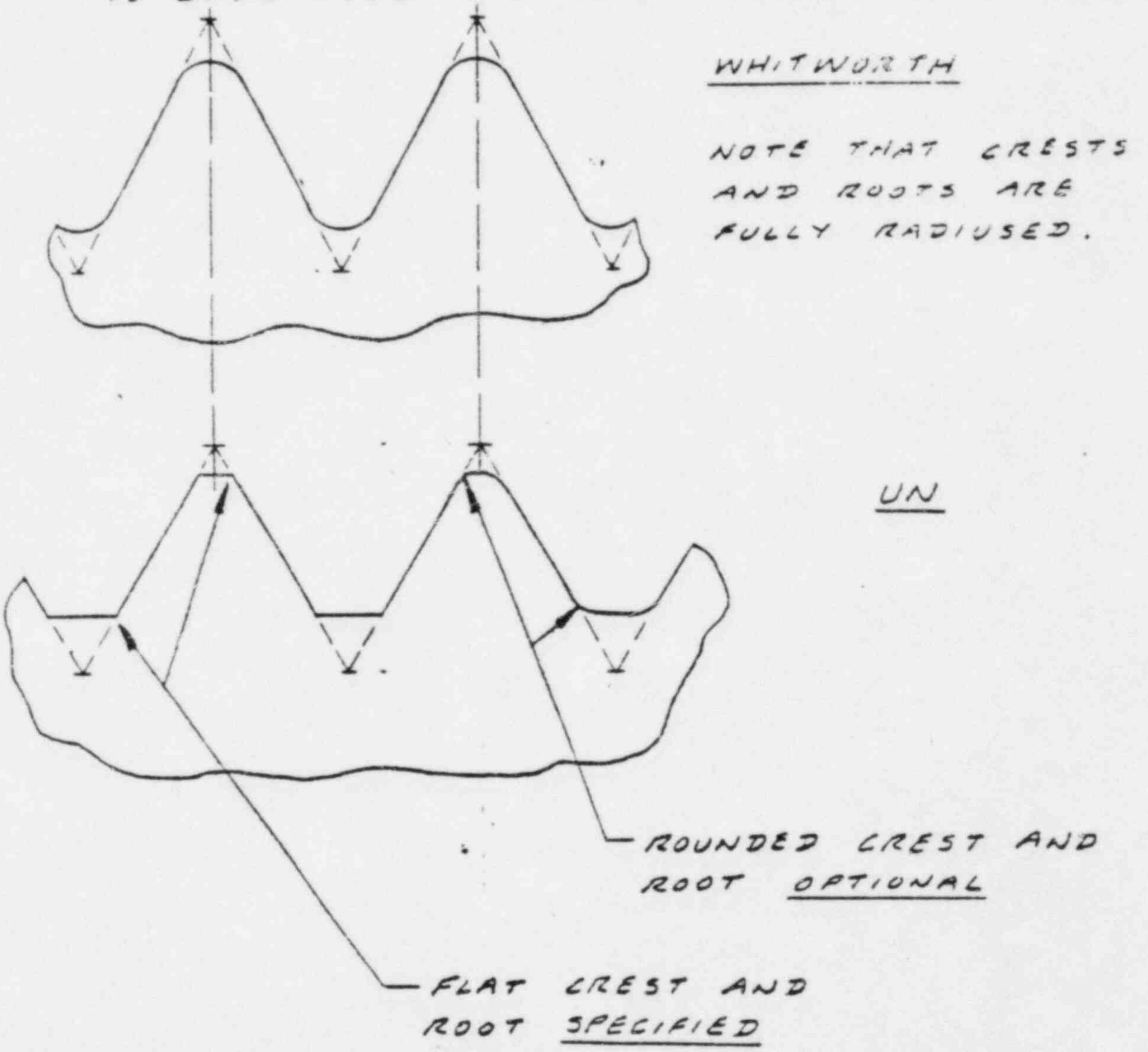
COMPARISON OF WHITWORTH AND UN THREAD FORMS

THE REASON FOR MAKING THIS COMPARISON IS EXPLAINED ON THE PRECEDING PAGE.



NOTE: BECAUSE OF THEIR FULLY ROUNDED ROOTS, THE WHITFORD THREADS HAVE LESS STRESS CONCENTRATION AND GREATER FATIGUE STRENGTH THAN THE UN THREADS.

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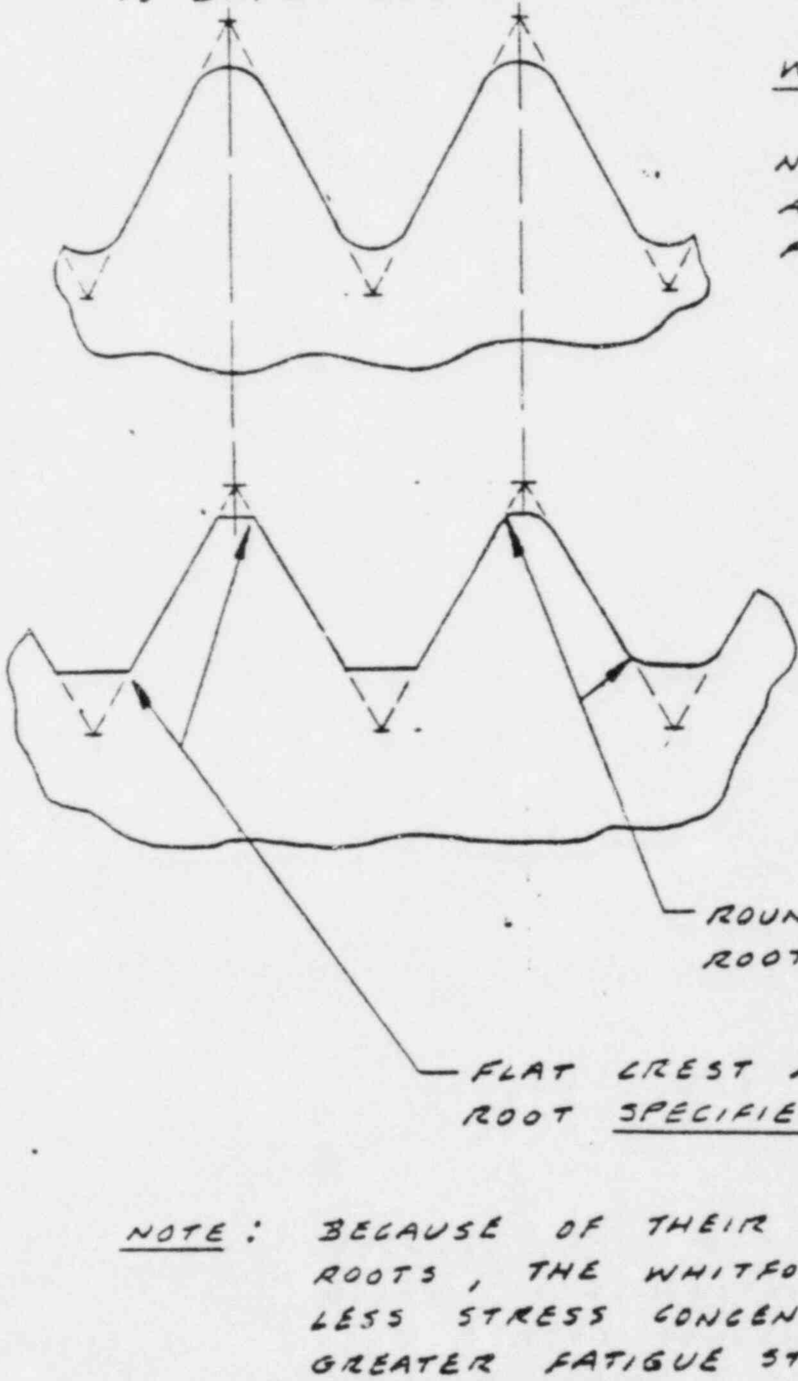

WHITWORTH

NOTE THAT CRESTS AND ROOTS ARE FULLY RADIUSED.

UN

ROUNDED CREST AND ROOT OPTIONAL

FLAT CREST AND ROOT SPECIFIED

NOTE: BECAUSE OF THEIR FULLY ROUNDED ROOTS, THE WHITFORD THREADS HAVE LESS STRESS CONCENTRATION AND GREATER FATIGUE STRENGTH THAN THE UN THREADS.

STUD STRESS CAUSED BY
PRELOAD PLUS OPERATING PRESSURE

IN ORDER TO TAKE INTO ACCOUNT THE SPRING CONSTANTS OF STUDS AND CLAMPED MEMBERS, LET US USE THE ANALYTICAL METHOD OF SECTION 14.16 ON PAGE 308 OF REFERENCE [5]. THE SAME METHOD IS GIVEN ON PAGE 167 OF REF [2].

D_o = O.D. OF CONTACT SURFACE OF FLANGE

= 25.75 IN.

D_i = I.D. OF CONTACT SURFACE OF FLANGE

= 18.38 IN.

A_{f_g} = GROSS CONTACT AREA OF FLANGE

$= .7854 (D_o^2 - D_i^2)$

= 255.4 IN.^{2}

N = NUMBER OF STUDS

= 16

d_h = DIAMETER OF BOLT HOLES

= 2.125 IN.

A_h = AREA OF BOLT HOLES

$= .7854\, d_h^2 N$

= 56.7 IN.^{2}

A_c = NET CONTACT AREA OF FLANGE

$= A_{fg} - A_h$

= 198.7 IN.^{2}

STUD STRESS CAUSED BY PRELOAD PLUS OPERATING PRESSURE, CONT

L_c' = THICKNESS OF MANWAY COVER

= 5.50 IN.

L_c'' = ONE-HALF OF LENGTH OF THREAD ENGAGEMENT IN MANWAY FLANGE

≅ 1.50 IN.

L_c = THICKNESS OF CLAMPED MEMBERS

$= L_c' + L_c''$

= 7.00 IN.

E = YOUNG'S MODULUS OF ALL MEMBERS

K_c = SPRING CONSTANT OF CLAMPED MEMBERS

$= A_c E / L_c$

= 28.39 E LB/IN.

A_b = TENSILE STRESS AREA OF SIXTEEN 2.00-8UN-2A STUDS

= 16 × 2.77 = 44.32 IN.^{2}

L_b = EFFECTIVE LENGTH OF STUDS

≅ 8.00 IN.

K_b = SPRING CONSTANT OF STUDS

$= A_b E / L_b$

= 5.54 E LB/IN.

$R = K_c / K_b = 5.12$

F_i = PRELOAD FORCE

P. 8

$= N S_{b_i} A_b$ = 16 × 31820 × 2.77

= 1,410,262 LB

STUD STRESS CAUSED BY PRELOAD PLUS OPERATING PRESSURE, CONT

L_c' = THICKNESS OF MANWAY COVER

= 5.50 IN.

L_c'' = ONE-HALF OF LENGTH OF THREAD ENGAGEMENT IN MANWAY FLANGE

= 1.50 IN.

L_c = THICKNESS OF CLAMPED MEMBERS

$= L_c' + L_c''$

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$R = K_c / K_b = 5.12$

F_i = PRELOAD FORCE

P. 8

$= N S_{b_i} A_b = 16 \times 31820 \times 2.77$

= 1,410,262 LB

STUD STRESS CAUSED BY PRELOAD PLUS OPERATING PRESSURE, CONT

G = MEAN DIAMETER OF GASKET

= (16.50 + 18.03) / 2

= 17.26 IN.

P = OPERATING PRESSURE

= 2200 PSIG

F_e = HYDROSTATIC END FORCE

= $0.7854\ G^2 P$

= 514,750 LB

F_b = TOTAL LOAD OF STUDS

$$= F_i + \frac{1}{1+R} F_e = 1,410,262 + \frac{514,750}{1+5.12}$$

= 1,494,372 LB

S_{b_o} = STUD STRESS, NOMINAL

= F_b / NA_b = 1,494,372 / 16 × 2.77

= **33718 PSI**

$K_t S_{b_o}$ = STUD STRESS, CONCENTRATED

= 3.0 × 33718 = **101,154 PSI**

$F_b' = F_b / N$ = 1,494,372/16 = **93400 LB PER STUD**

BECAUSE THE BOLTED JOINT IS VERY RIGID, OPERATING PRESSURE INCREASES STUD STRESS ONLY 6.0 % MORE THAN THE PRELOAD VALUE.

THAT IS, S_{b_o} / S_{b_i} = 33718/31820

= **1.060**

14 15

STUD STRESS CAUSED BY DESIGN PRESSURE ONLY

G = MEAN DIAMETER OF GASKET

= (16.50 + 18.03)/2

= 17.26 IN.

P = DESIGN PRESSURE

= 2500 PSIG

H = TOTAL HYDROSTATIC END FORCE

= $0.7854\, G^2 P = 0.7854 \times \overline{17.26}^2 \times 2500$

= 584,942 LB

A_b = TENSILE STRESS AREA
OF 2.000-8UN-2A STUD

= 2.77 $IN.^2$

N = NUMBER OF STUDS PER MANWAY

= 16

S_{b_d} = TENSILE STRESS OF STUD CAUSED
BY DESIGN PRESSURE ONLY

= H/NA_b = 584,942/16 × 2.77

= <u>13198 PSI</u> < $S_{ALLOWABLE}$ = 25000 PSI
FOR SA-320 GRADE L43
REF [3], PAGE 135

W. FOLEY 1-14-81 ATTACHMENT 1 15 15
RSD 3-30-81 OF REPORT IE-123 05-80-251
PARAMETER, INC. NRC-IE-80/31
TASK 03

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2. FAIRES, VIRGIL MORING, DESIGN OF MACHINE ELEMENTS, FOURTH EDITION, THE MACMILLAN COMPANY, NEW YORK

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4. PETERSON, R.E., STRESS CONCENTRATION DESIGN FACTORS, FIFTH PRINTING, JOHN WILEY & SONS, INC., NEW YORK

5. JUVINALL, ROBERT C., ENGINEERING CONSIDERATIONS OF STRESS, STRAIN AND STRENGTH, 1967, MCGRAW-HILL BOOK COMPANY, NEW YORK

6. BABCOCK & WILCOX COMPANY, CRACKED STEAM GENERATOR MANWAY STUDS AT OCONEE III, LETTER OF JULY 11, 1980 TO VICTOR STELLO (NRC/OIE) FROM JAMES H. TAYLOR (B&W/PGG)

ATTACHMENT 2

FRACTURE MECHANICS ANALYSIS OF MANWAY STUDS

by L. H. Burck

Introduction

This attachment presents an analysis to determine the critical defect size for stress corrosion cracking in the thread root region of the subject manway studs and to determine the critical crack size for final fracture of a stud. The nominal stresses and stress concentrations utilized in this analysis are those which were determined by the stress analysis presented in Attachment 1 of this report.

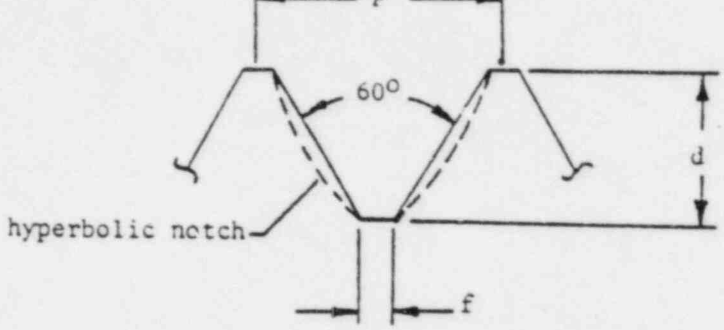
Summary of Results

1. The critical defect size for the onset of stress corrosion cracking at the roots of threads in the subject studs is 0.070 inches for normal operational loading, exclusive of any safety factors.
2. The critical crack size for final fracture of the studs is 0.46 inches beyond the thread root for normal operational loading, exclusive of any safety factors. This value does not reflect the unloading effects of prior crack propagation by stress corrosion which would result in a larger critical crack size for final fracture but could also accelerate cracking in neighboring studs.

Analysis

Stress Intensity Factor

In calculating the stress intensity factor for a crack emanating from the root of a stud thread, the thread form is modeled as a hyperbolic notch having the same depth, width, and stress concentration factor as a thread in the non-engaged portion of the stud.



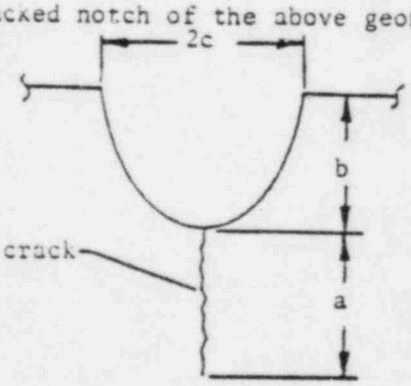
For a 2-8UN-2A thread (1),

$$p = 1/8 = 0.125 \text{ in.}$$

$$f = p/8 = 0.125/8 = 0.0156 \text{ in.}$$

$$d = (2.0 - 1.847)/2 = 0.0765 \text{ in.}$$

Stress concentration and stress intensity factor solutions for an isolated cracked notch of the above geometry are presented in (2), page 19.13,



$$2c = p - f = 0.125 - 0.0156 = 0.109 \text{ in.}$$

i.e., $c = 0.0545$ in.

$$b = d = 0.0765 \text{ in.}$$

The stress intensity factor is given by,

$$K_I = \sigma\sqrt{\pi a}\ F(a/b, c/b)$$

where, σ: nominal stress based on gross area (major diameter),

load per stud = 93,400 lbs. (Attachment 1),

gross area = $\pi(2.0)^2/4 = 3.14$ in.^{2},

$\sigma = 93{,}400/3.14 = 29{,}700$ psi for normal operating conditions.

F(a/b,c/b): function of a/b and c/b which is given in graphical form in (2), page 19.13.

The stress concentration factor for a isolated notch of this geometry is also given by (2), page 19.13,

$$k_t = (1 + 2b/c)\left(1 + 0.122\{1/(1 + c/b)\}^{2.5}\right)$$

$$k_t = 3.93$$

However, because the thread form represents a series of notches, the stress concentration factor is less than for an isolated notch of the same dimensions; in the present case $k_t = 3.0$ (see Attachment 1). Thus the factor $\alpha = 3.0/3.93$ is incorporated into the stress intensity factor expression (in the limit for very small cracks, $F = 1.12k_t$), i.e.,

$$K_I = \alpha\sigma\sqrt{\pi a}\ F(a/b,c/b).$$

In general, this expression is set equal to a critical value of the stress intensity factor, such as the threshold level for stress corrosion cracking, and the critical crack size is determined by a numerical iteration procedure.

Stress Corrosion Cracking

Stress corrosion cracking of a material in a particular environment may be expected when the stress intensity factor, K_I, equals or exceeds the threshold value for stress corrosion cracking, K_{ISCC}, for that material and environment. Literature values of K_{ISCC} for quenched and tempered AISI 4340 steel tested at room temperature in a sulfide/chloride environment range from 17.5 ksi$\sqrt{in.}$ for a tempered hardness of R_C 39.1 to a value of 40 ksi$\sqrt{in.}$ for a hardness of R_C 29.4 (3). A value of K_{ISCC} = 17.5 ksi$\sqrt{in.}$ is therefore

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In general, this expression is set equal to a critical value of the stress intensity factor, such as the threshold level for stress corrosion cracking, and the critical crack size is determined by a numerical iteration procedure.

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$$K_I = K_{ISCC} = 17.5 \text{ ksi}\sqrt{\text{in.}}.$$

The critical defect size for the initiation of stress corrosion cracking, a_{scc}, is thus calculated from the previously discussed stress intensity factor expression, yielding,

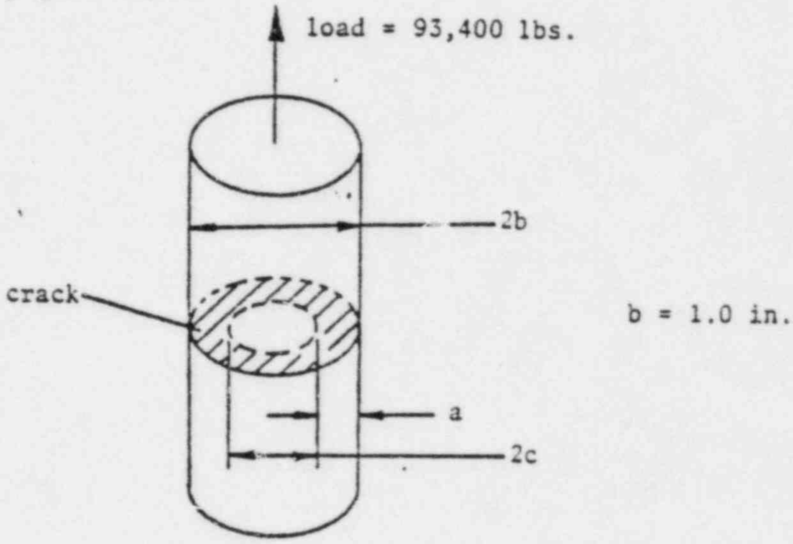
$$a_{scc} = 0.070 \text{ inches},$$

for normal operating conditions, exclusive of any factors of safety on either the calculated stress intensity factor or the assumed value of K_{ISCC}. It is noted that, because of the stress concentration effects of the threads, a safety factor applied to the stress intensity factor has a considerably greater effect on the calculated critical defect size than would be the case for a surface defect not associated with a notch or other stress concentration. For example, a safety factor of 2.0 applied to K_{ISCC} results in the calculated value of a_{scc} being reduced to 0.003 inches in the present analysis whereas for surface cracks away from stress concentrations, the calculated critical defect size would be reduced by a factor equal to the square of the safety factor, i.e., by a factor of 4.0 in this example.

Final Fracture

Final fracture will occur by a brittle mode when the stress intensity factor of the stress corrosion crack reaches the fracture toughness of the stud material. However, a stud will effectively have failed if net section yielding occurs prior to that.

In the case of final failure, the crack sizes involved are substantially larger than for the initiation of stress corrosion cracks and, therefore, the previously used stress intensity factor is not applicable as the finite size of the stud bolts must be taken into account. Conversely, the stress concentration of the threads will have little effect on the final critical crack size. The case of final fracture is modeled as the stud being a circumferentially cracked cylinder, the stress intensity factor for which is given by (2), page 27.1,



$$K_I = \sigma_{net}\sqrt{\pi a}\, F_2(c/b)$$

where, σ_{net} : net section stress, $\sigma_{net} = 93,400/(\pi c^2)$

F_2 : function of (c/b) which is given in numerical form in (2), page 27.1.

For AISI 4340 steel heat treated to a room temperature yield strength level of 165 ksi, a representative fracture toughness is K_{IC} = 110 ksi$\sqrt{in.}$ (4). Utilizing this value with the above stress intensity factor expression yields a = 0.621 inches for brittle fracture. Subtracting the depth of the thread gives a critical crack dimension of 0.54 inches beyond the thread root, exclusive of any factors of safety. This calculation does not reflect load relaxation effects which might occur as a result of prior stress corrosion cracking. Such bolt unloading would result in increased critical crack sizes if the studs were not re-torqued subsequent to stress corrosion cracking. However, such unloading would transfer pressure loading to adjacent studs and could thereby accelerate cracking in those studs.

At the critical crack size for brittle fracture indicated above, the (hypothetical) net stress would be given by,

$$\sigma_{net} = 93{,}400/(\pi(1 - .621)^2) = 207{,}000 \text{ psi},$$

whereas the studs tested showed room temperature yield strengths as low as 145 ksi and tensile strengths of 158 ksi. At a temperature of 550°F, these yield and tensile strengths would be approximately 140 ksi and 150 ksi, respectively (5). Therefore, overload failure of the studs would be expected to occur prior to the brittle fracture considered above. Considering that loss of preload would effectively occur upon net section yielding, the criterion for failure is given as,

$$\sigma_{net} = \text{yield stress}$$

$$93{,}400/\pi(1 - a)^2 = 140{,}000 \text{ psi}$$

which gives a = 0.54 inches or a crack depth of 0.46 inches beyond the thread root, exclusive of a factor of safety. Again, this calculation does not consider the effects of unloading due to prior crack growth which could result in larger critical crack sizes if the studs were not re-tightened.

References

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5. Metals Handbook, Volume 1, Eighth Edition, American Society for Metals, 1964.

Checked by: Robert S. Dean

Robert S. Dean, P.E.

STUD STRESS CAUSED BY PRELOAD PLUS OPERATING PRESSURE, CONT

L_c' = THICKNESS OF MANWAY COVER

= 5.50 IN.

L_c'' = ONE-HALF OF LENGTH OF THREAD ENGAGEMENT IN MANWAY FLANGE

= 1.50 IN.

L_c = THICKNESS OF CLAMPED MEMBERS

$= L_c' + L_c''$

= 7.00 IN.

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K_c = SPRING CONSTANT OF CLAMPED MEMBERS

$= A_c E / L_c$

$= 28.39\ E$ LB/IN.

A_b = TENSILE STRESS AREA OF SIXTEEN 2.00-8UN-2A STUDS

$= 16 \times 2.77 = 44.32$ IN.^{2}

L_b = EFFECTIVE LENGTH OF STUDS

$\approx$ 8.00 IN.

K_b = SPRING CONSTANT OF STUDS

$= A_b E / L_b$

$= 5.54\ E$ LB/IN.

$R = K_c / K_b = 5.12$

F_i = PRELOAD FORCE

P. 8

$= N S_{b_i} A_b = 16 \times 31820 \times 2.77$

= 1,410,262 LB