PREDICTION OF TEAR RESISTANCE OF SOME ALUMINUM AND STEEL ALLOYS BY SMALL SCALE TENSILE TESTS

by

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ABSTRACT

The use of a new method of measuring the "fracture toughness" of a material (see McClintock, 1960 and 1961) has been investigated for thin sheets of the aluminum alloys Alclad 2024-T3, 2024-T3, 6061-T6, and 5456-H24 and the stainless steel alloy AM350-CRT. The results have been compared with data from large scale slotted panels to get a measure of its validity.

Although the results from the two types of tests differ by a factor of two, it is put forth that causes of the errors are correctible. Suggestions for further testing are included.

ACKNOWLEDGEMENT

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INTRODUCTION

With the advent of today's powerful aircraft and space vehicles, designers found that the requirements of minimum possible weight led to the use of very high strength materials. Metallurgists helped by developing steels with ultimate tensile strengths close to 300000 pounds per square inch and aluminum alloys with ultimate strengths over 80000 psi. Unfortunately, these high strengths are almost inevitably associated with lower ductility and higher "notch sensitivity." The result is that a small, almost imperceptible crack could precipitate complete failure of a structure at a stress well below the nominal tensile strength of the material, as was the case in the comet crashes. For tensile loaded structures, this resistance to crack propagation failure, rather than ultimate tensile strength, is therefore frequently the criterion for the selection of both the material and the design allowable stresses.

Many projects sponsored by government agencies and aircraft manufacturers, both of whom have considerable interests in these material limitations, have been formed to investigate "fracture toughness." The main efforts have been, 1) to correlate experimental results with some theoretical model and thus predict the resistance a material will offer to these crack propagation failures and 2) to evaluate the effects of changes in materials and structures on this resistance.

BACKGROUND

The crack concept advanced by Griffith in 1921, originally proposed for the failure of glass and later modified by Orowan, 1955, and others to apply to structural metals, can be thought of as either an energy balance or a stress or strain concentration concept. For biaxial stress situations, it states that an existing crack of length C will propagate cataclysmically if the strain energy release due to an increase in crack length is larger than the accompanying energy absorption by the newly created surface and local plastic deformation at and near the tip of the crack. For a crack of length C in an infinite plate this concept is expressed by the equation

$$\sigma \sqrt{C} = \text{constant}$$

where σ is the stress far away from the crack at which fracture occurs. Orowan has shown, for an elastic solid, that the Griffith condition for crack propagation is not only a necessary but also a sufficient condition for brittle fracture.

By a stress analysis for edge and center cracks in finite specimens, Irwin has been able to extend the applicability of the Griffith concept to typical engineering test specimen geometries. This modified Griffith concept, also termed fracture mechanics, is expressed in terms of a material constant or parameter which describes either a critical energy release rate, G_c, or a critical stress state, K_c, under which cataclysmic crack propagation occurs. These critical values are functions of the specimen geometry and applied stress system only. They are mutually interrelated by the equation

$$K_c^2 = E\, G_c$$

where E is Young's modulus. The equations defining G_c and K_c for two pertinent cases are given below from ASTM, 1960, and Kies, 1956.

for an infinite plate:

$$G_c = \frac{\pi \sigma^2 C}{2\,\text{[redacted]}\,E} \qquad \text{and } K_c = \sigma\sqrt{\frac{\pi}{2}\frac{C}{\text{[redacted]}}}$$

for a finite plate of width b:

$$G_c = \frac{\pi \sigma^2 C \alpha^2}{\text{[redacted]}\,E} \qquad \text{and } K_c = \alpha\sigma\sqrt{\frac{\pi}{\text{[redacted]}}\frac{C}{\text{[redacted]}}}$$

where σ is the gross average stress based on the overall specimen dimensions and α is given by

$$\alpha^2 = \frac{2 + (c/b)^4}{\left(2 - (c/b)^2 - (c/b)^4\right)^2}$$

In fracture mechanics plastic deformation at the tip of a crack is accounted for by modifying the crack length, C. The argument for this is that since the load carrying ability of a test specimen is reduced due to plastic flow at the crack tips, the crack length is increased by the length of the plastic zone. For an estimate of this correction the plastic zone , r_y, is assumed to be the region for which the normal stress exceeds the yield strength, σ_{ys}. For the Griffith crack the plastic zone is given in ASTM, 1960, as

$$r_y = \frac{K^2}{2\pi(\sigma_{ys})^2}$$

so that the stress field parameter becomes

$$K_c^2 = \frac{\sigma^2 \pi C}{2\left[1 - \text{[redacted]}(\sigma/\sigma_{ys})^2\right]}$$

Since the above described analytical approach is based on a two dimensional elastic stress analysis, it must not be expected to be applicable over a wide range of sheet thicknesses. Accordingly, the actual stress state at the tip of a crack may vary from a state of plane stress, for very thin specimens, to a state of plane strain for thick specimens, as discussed by Irwin, 1960.

A more detailed development of this concept can be found in ASTM, 1960.

To avoid this somewhat awkward method of adopting an elastic stress analysis to include the plastic zone at the tip of the Griffith crack, an elastic-plastic analysis would be needed. Unfortunately, formulating such an analysis for a specimen in tension has not yet been accomplished. Pending this, McClintock, 1961, has suggested an analogy with the purely longitudinal shear case where a solution at least partially verified by experiment has been found as stated in McClintock, 1958. The results of this analogy are that, for plane stress,

$$\frac{R}{C} = f\left(\frac{\sigma}{TS}\right)^2$$

where, again, R is the radius of the plastic zone and C is the crack length. The critical value of this R for instability of sheet, R_c, would serve the same purpose as Irwin's K_c or G_c as a material constant giving a measure of the fracture toughness of a material. It should also allow prediction of the stress at which a material with a crack length and thickness will fail.

Comparing this with equation (5) in ASTM, 1960,

$$R_c = f\left[\frac{K_c^2\,\text{[redacted]}}{(TS)^2\pi}\left\{1 - \text{[redacted]}(\sigma/\sigma_{ys})^2\right\}\right]$$

For low stress levels this reduces to

$$R_c = \frac{K_c^2 \text{[redacted]}}{\pi (TS)^2}$$

but, with the aid of graph 1 , R_c can be approximated at any stress level. Likewise, this analogy predicts that, for instability of sheet,

$$R_{cs} = B(R.A.) \ \exp.\left[\left\{2 \left(\frac{\varepsilon_u E}{TS} - 1\right)\right\}^{1/2} -1\right]$$

Here B is the sheet thickness and RA is the reduction of area for specimen 1, which appears in the appendix. TS is the tensile strength and ε_u is the uniform strain, that just before necking begins, in an unnotched strip.

There seem to be **several** advantages to this elastic-plastic analysis. It shows why cracks that are initially stable become unstable; it relates the notch sensitivity to other physically measurable quantities, and it describes the notch sensitivity in terms of the critical radius to the elastic-plastic boundary, R_c, rather the less tangible K_c. Also, in the plane stress regime, the radius of the plastic zone can be predicted by making simple tests involving only the uniform strain and ultimate strength in a tensile specimen and the reduction in area in a specimen where necking was confined to be normal to the applied load. However, to prove useful, any such theory must agree with experimental evidence; it is the purpose of this thesis to investigate this correlation.

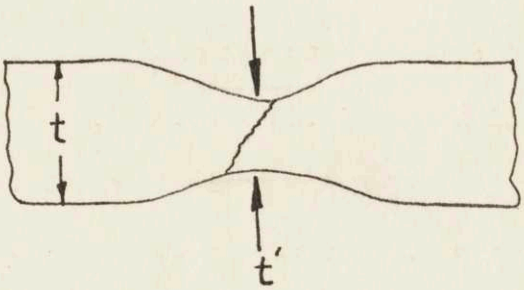
PROCEDURE

To make possible the testing of more alloys, it was decided to make use of the large amount of data existent in industry about the notch sensitivity of large sheets. Accordingly, requests were sent to several aircraft manufacturers and aluminum producers asking for data on large scale tests of panels of aluminum and steel containing an original crack, and for samples of material. Because of the variation of properties in samples of a given alloy produced at different locations, or even in samples from different heats, specimens taken from the panels that had been tested as described above were requested. No such material was available, except in the case of the stainless steel AM 350, but both specimens and data were received from one producer, Alcoa.

A specimen failing in plane stress experiences a pure shear fracture. Data from Stapleton, 1960, showed that, in the thicknesses tested (.050 and .063 inches), the aluminum alloy 2024-T3 was found to have a 100% shear failure. However, no other mention could be found of fracture appearance so alloys having a value of K_c equal to or greater than that of 2024-T3 were singled out as those whose mode of fracture would most closely approximate the plane stress case. The alloys tested were 2024-T3 and 5456-H24, in .063" thicknesses, and Alclad 2024-T3 and 6061-T6, in both .050" and .063" thicknesses. Also the stainless steel AM350 CRT, .020 inches thick, was investigated.

The specimens used to determine the RA and the TS and ε_u are shown as specimens (1) and (2) respectively in figure 2 . These strips were easy to machine, but care was taken in positioning and drilling the holes in specimen (1) and in milling the reduced section of specimen (2). Only a few of the tensile strips varied longitudinally by more than .001 inch. in the reduced section. At least two tests were made on each configuration of each alloy and thickness.

All tests were run on the Tinius Olsen 12000 pound tensile test machine operated by the Metallurgy Department. Care had to be taken on this particular machine to align the specimen in the grips to avoid bending. The estimated error of load readings was less than $\pm$ 10 pounds on the 12000 pound scale. The reduction of area was determined in two different ways; the first method was to measure across the fracture, which usually occurred along a 45° shear plane, with a point micrometer:



$$\frac{t-t'}{t} = R.A.$$

Figure 1

These results were checked by magnifying and measuring the reduced area with the calibrated eye-piece of a Unitron microscope. No significant differences in the results of the two methods was found.

There was, however, some difficulty in measuring the uniform strain before necking began in a tensile strip, ε_u. Any notches on the surface, like those made by 2 inch gauge marks, was found to induce failure in the immediate area of the notch in the 2024-T3 alloys because of stress concentrations. An approximate method of measuring the thickness and width of the reduced strip of the tensile specimen before and after the test was finally used to find an average value of ε_u . To provide a check for this, two samples of each alloy were tested with an electronic extensionmeter attached. This verified the data on all alloys except 5456-H24, where the alloy's peculiar yielding process seemed to interfere with the readings. The mechanics of its failure resemble that of Luder's lines in some steels, but it is characterized by a number of such lines being initiated in the reduced section and moving up into the gripping areas; this is a local yielding process that seems to have been accentuated in the region of the contact points of the extensionmeter.

RESULTS

Values of R_c as calculated from 1) large panels with an original crack and 2) the tensile strips shown in figure 2 are tabulated in Table 3.

With the variation that is found in different samples of the same alloy due to small differences in composition and heat treatments, it is most instructive, in the case of aluminum alloys, to compare the values from Alcoa data (Kaufman, 1960) and Alcoa material (designated A in the table). In all cases the R_c calculated from the small tests was greater than the actual value, the difference being greatest in the 2024-T3 alloys and least for the 6061-T6 alloy. The results for the stainless steel AM350-CRT, the only material tested for which tear resistance data for specimens of the same heat was available, showed no significant difference in the results of the two methods.

The effect of stiffening the specimens by clamping, or welding, extra material parallel to the crack direction can be seen (Tables 1,3) by comparing the respective results for AM350. The braces may be thought of as increasing the energy available in the material for resisting cracks and, hence, increasing the "fracture toughness" of the material. In Table 1 a characteristic difference in G_c value between transverse and longitudinal specimens of the same alloy is shown (again in the case of the AM350 alloy). However, here the results represent the work of two different investigators and it is difficult to say whether the variations come in the specimens or in the methods of the investigators. The latter was a general problem since each investigator expressed his results in different terms or detail and it was sometimes difficult to compare their results. The ASTM, 1960, report is the first attempt to bring order into this reporting of data and should result in improved communication of results.

G_c, and hence R_c, calculations are based on the critical crack length, but, since this data was not available in the Alcoa, (Kaufman, 1960) and Douglas (Pendleton, 1958 and 1960) reports, approximate values of these two parameters were determined using the original crack length for C. Since all of these alloys experience some stable crack propagation prior to final fracture, up to 20% in Alclad 2024-T3 (as reported in Kinsel, 1960), the $R_c^{(1)}$ value calculated from this data are lower than they should be by a factor of this order of magnitude. Also a G_c represented in table (1) as calculated from Alcoa data is an average of values for four crack lengths. For the alloy 2024-T3 this represents G_c's of from 1420 inch pounds per square inch for a 6 inch crack length to 350 inch pounds per square inch for a 1 inch crack length indicating that the correction factor for specimen dimensions from Kies, 1956, is not sufficient for large variations in these dimensions. Also, there is evidence that the approximations in ASTM, 1960, for the effect of plastic deformation at the crack tip is less accurate for the higher levels of σ / σ_{YS}. at which the aluminum alloys failed. Again in Alclad 2024-T3, it was found that the R.A. of a .040 inch panel with an original crack that had been fractured, was only on the order of one-half the R.A. predicted by specimen (2). This is not easy to understand since once a crack starts growing it should look the same to the material whether it was initiated by a jewler's saw cut or by a notch with a large nose radius.

Since McClintock's analogy uses this reduction of area to represent the area of the material that is deforming plastically, the specimen is not measuring the parameter it should be. It would be of interest to compare these values of R_c for different alloys to see if this effect is important in all cases.

The equation for R_c (see page 5) with its exponential dependence on ε_u is also a limit to the possible accuracy of these values. The differences in R_c's[2] for the same alloy as calculated using Reynolds and Alcoa stock can be traced primarily to a small difference in their respective uniform strains. This may be a major limitation in the use of this method since, even with no variations in heats, the ε_u as recorded on two different testing machines might be great enough to change the meaning of the results.

Although pertinent data was not available, it is plausible that some or all of these factors also played a role in the failure of the other aluminum alloys, and that these could be used to explain a large part of the differences found in R_c.

Tests of this sort have been conducted previously (see McClintock, 1960) with somewhat better results. In graph 2 , taken from a thesis by Hirschberg, 1961, the R_c value for 0.1 inch thick strips of 4340 steel as determined by McClintock's and Irwin's methods are plotted against tempering temperature. In the region where 100% shear failure was experienced, at tempering temperatures greater than 550°F, the agreement was very good. In this case the specimens were also taken from the same heat, indicating, along with the data for AM350, that this is an important consideration.

It should also be noted that the relative ranking of the materials is different when made by their R_c values than when made by their G_c values.

RANK OF MATERIALS

by G_c	by R_c
Alclad 2024-T3	6061-T6
2024-T3	5456-H24
6061-T6	Alclad 2024-T3
5456-H24	2024-T3

However, this inconsistency is also found in comparison among other tests that also claim to measure "fracture toughness." Of final importance here is how these materials actually behave in service and without sufficient data it would be impossible to say which of the above rankings is closest to actual results.

These results do more toward pointing out the limitations of this method of measuring the fracture toughness of a material than toward proclaiming its success. But it does emphasize some important considerations for further testing.

1) Material for both the large and small scale tests should come from the same heat of metal. This would not be a great restriction on its utility since individual small tests could be made on each sheet intended for a critical application without much trouble.

2) For the most meaningful comparison tests should be standardized using dependable equipment to give the best measure of important factor such as ε_u.

APPENDIX

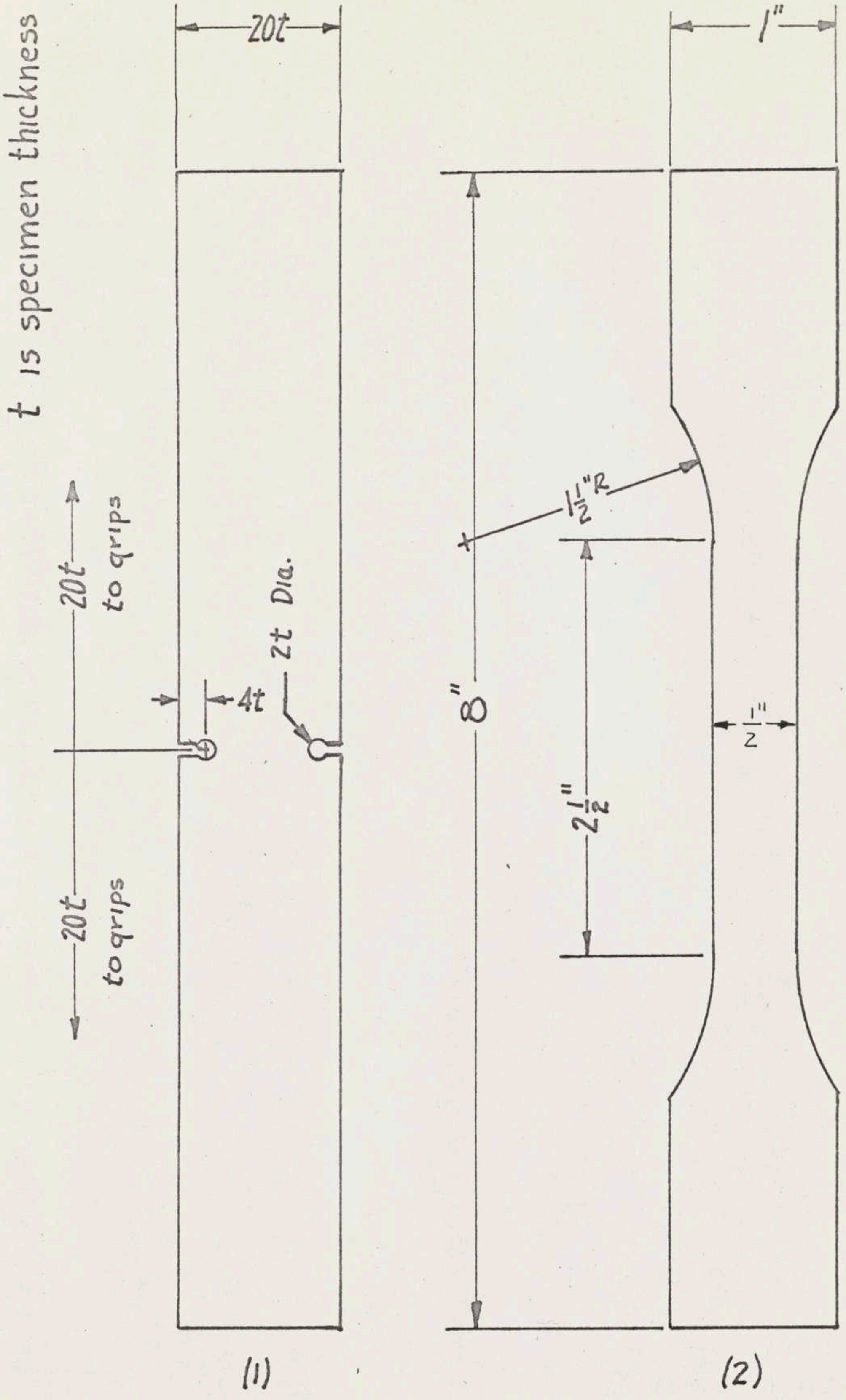
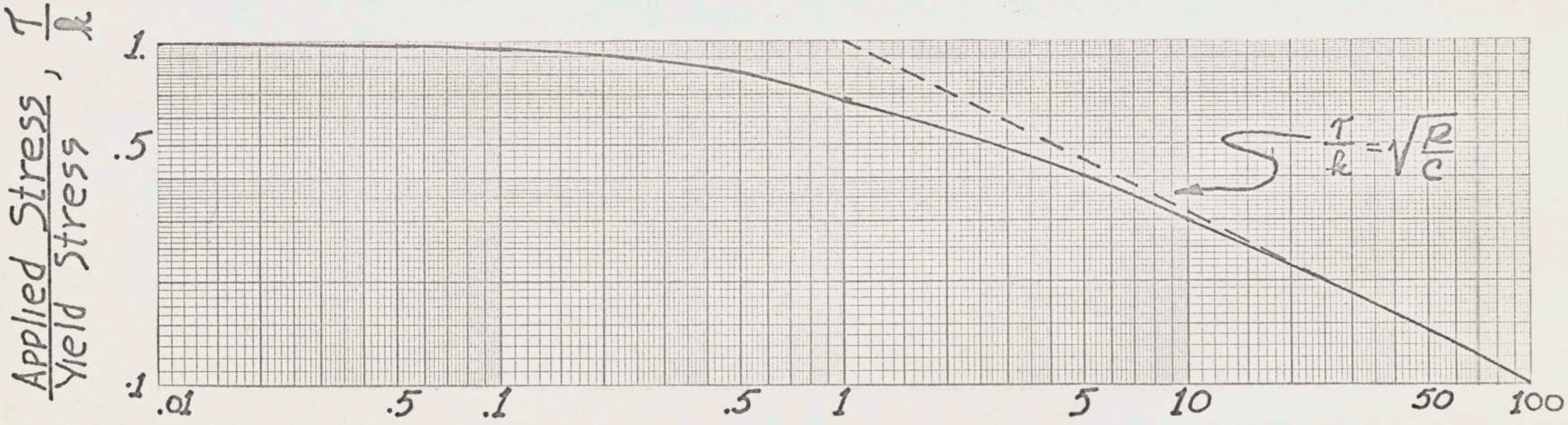


Fig. 2

Tensile Specimens

Dependence of Radius of Plastic Zone on Applied Stress



Crack Length / Radius of Plastic Zone, $\frac{C}{R}$

GRAPH #1

TABLE 1

DATA FROM TENSILE SPECIMENS

ALLOY	SPEC. #	THICKNESS (in.)	R.A.	TS (KSI)	ε_u^p	SOURCE OF MATERIAL
A12024-T3	5	.050	.109	——	——	REYNOLDS
A12024-T3	6	.050	.148	——	——	
A12024-T3	7	.050	——	62.0	.115	
A12024-T3	8	.050	——	62.4	.120	
A12024-T3	27	.050	——	63.0	.130	
A12024-T3	28	.050	——	63.0	.134	
A12024-T3	9	.063	.175	——	——	
A12024-T3	10	.063	.143	——	——	
A12024-T3	11	.063	——	64.9	——	
A12024-T3	12	.063	——	65.4	.145	
A12024OT3	25	.063	——	64.5	.157	
A12024-T3	26	.063	——	64.5	.160	
A12024-T3	37	.063	——	66.0	.135	ALCOA
A12024-T3	38	.063	——	65.0	.135	
A12024-T3	39	.063	.136	——	——	
A12024-T3	40	.063	.152	——	——	
A12024-T3	49	.063	——	65.5	.160	
A12024-T3	50	.063	——	65.5	.170	
2024-T3	13	.064	.175	——	——	REYNOLDS
2024-T3	14	.064	.169	——	——	
2024-T3	15	.064	——	69.3	.118	
2024-T3	16	.064	——	68.7	.165	
2024-T3	23	.064	——	69.5	.184	
2024-T3	24	.064	——	69.7	.178	
2024-T3	45	.064	——	67.2	.105	ALCOA
2024-T3	46	.064	——	68.0	.140	
2024-T3	47	.064	.178	——	——	
2024-T3	48	.064	.147	——	——	
2024-T3	51	.064	——	66.6	.120	
2024-T3	52	.064	——	65.0	——	
6061-T6	1	.050	.24	——	——	REYNOLDS
6061-T6	2	.050	.20	——	——	
6061-T6	3	.050	——	47.0	.085	
6061-T6	4	.050	——	44.7	.075	
6061-T6	17	.063	——	49.0	.12	
6061-T6	18	.063	.246	——	——	
6061-T6	19	.063	.246	——	——	
6061-T6	20	.063	——	48.8	.11	

TABLE 1 (continued)

ALLOY	SPEC. #	THICKNESS (in.)	R.A.	TS (KSI)	ε_u^p	SOURCE OF MATERIAL
6061-T6	29	.063	——	47.3	.080	ALCOA
6061-T6	30	.063	——	47.2	.075	
6061-T6	31	.063	.238	——		
6061-T6	32	.063	.254	——		
6061-T6	53	.063	——	46.2	.09	
6061-T6	54	.063	——	46.2	.10	
5456-H24	33	.064	——	55.1	.090	ALCOA
5456-H24	34	.064	——	55.4	.085	
5456-H24	35	.064	.132	——	——	
5456-H24	36	.064	.147	——	——	
5456-H24	55	.064	——	55.8	.130	
AM350CRT	41	.0205	——	225.0	.130	BOEING
AM350CRT	42	.0205	——	218.0	.130	
AM350CRT	43	.0205	.17	——		
AM350CRT	44	.0205	.22	——	——	

TABLE -2-

SMALL SCALE DATA CONDENSED

ALLOY	THICKNESS (inches)	REDUCTION IN AREA	TENSILE STRENGTH (KSI)	UNIFORM STRAIN ε_u	SOURCE OF MATERIAL
2024-T3 bare	.064 .064	.162 .167	67.3 69.3	.13 .17	Alcoa Reynolds
2024-T3 clad clad	.050 .063 .063	.128 .144 .159	62.6 65.5 64.8	.135 .15 .155	R A R
5456H24	.064	.139	55.4	.11	A
6061-T6 bare	.050 .063 .063	.22 .246 .246	45.9 46.7 48.9	.09 .095 .125	R A R
AM350	.0205	.195	222.0	.14	Boeing

COMPILATION OF DATA FROM LARGE NOTCHED PANELS

ALLOY	THICKNESS (inches)	WIDTH (inches)	TENSILE STRENGTH (KSI)	YIELD STRENGTH (KSI)	FAILURE STRESS (KSI) ǂ	ORIGINAL CRACK LENGTH (inches)	CRITICAL CRACK LENGTH (inches)	K_c KSI$\sqrt{in.}$	G_c $\left(\frac{in.lbs}{sq.\ in.}\right)$	SOURCE OF DATA (see references)
2024-T3	.050	30	68.0	46.0	—	—	—	130*	1700*	(13)
2024-T3	.050	24	68.0	46.0	—	—	—	102*	1040*	(13)
2024-T3	.050	9	68.0	46.0	—	—	—	82.2	675	(13)
2024-T3	.064	16	65.8	44.6	40.0(a)	(a)	—	—	830	(9)
Al-2024-T3	.040	30	64.3	43.2	18.1	15	17.4	—	1195	(10)
Al-2024-T3	.050	4	64.1	47.5	34.0(b)	(b)	—	—	615	(11)
Al-2024-T3	.063	48	66.5	44.3	17.6	24	26.9	—	1725*	(10)
Al-2024-T3	.063	16	66.3	46.6	40.4(a)	(a)	—	—	855	(9)
5456-H24	.063	16	55.0	38.6	33.8(a)	(a)	—	—	625	(9)
6061-T6	.050	4	46.8	41.2	30.3(b)	(b)	—	—	490	(11)
6061-T6	.064	16	45.4	40.8	36.7(a)	(a)	—	—	750	(9)
AM350-(L)	.0205	24	219.4	185.0	66.0	8	9.52	280	2770	(8)
AM350-(L)	.0205	24	219.4	185.0	93.3	8	9.6	400*	5570*	(8)
AM350-(L)	.025	4	200.0	170	63.5(b)	(b)	—	370	4730*	(12)
AM350-(L)	.025	12	206.0	172	—	—	—	374	4830*	(13)
AM350-[illegible]	.025	9	206.0	172	—	—	—	250*	2160	(13)
AM350-[illegible]	.025	20	206.0	172	—	—	—	276*	2700*	(13)

Notes:
- ǂ based on gross area.
- * specimen stiffened to prevent buckling.
- (a) data represents average for original crack lengths of 1,3,4 and 6 inches.
- (b) data represents average for original crack lengths of 1.0, 1.5 and 2.0 inches.
- E — Aluminum = 10^7 E — AM350 = 29×10^6
- all specimens transverse to grain direction unless marked (L)

TABLE -3-

TABLE 4

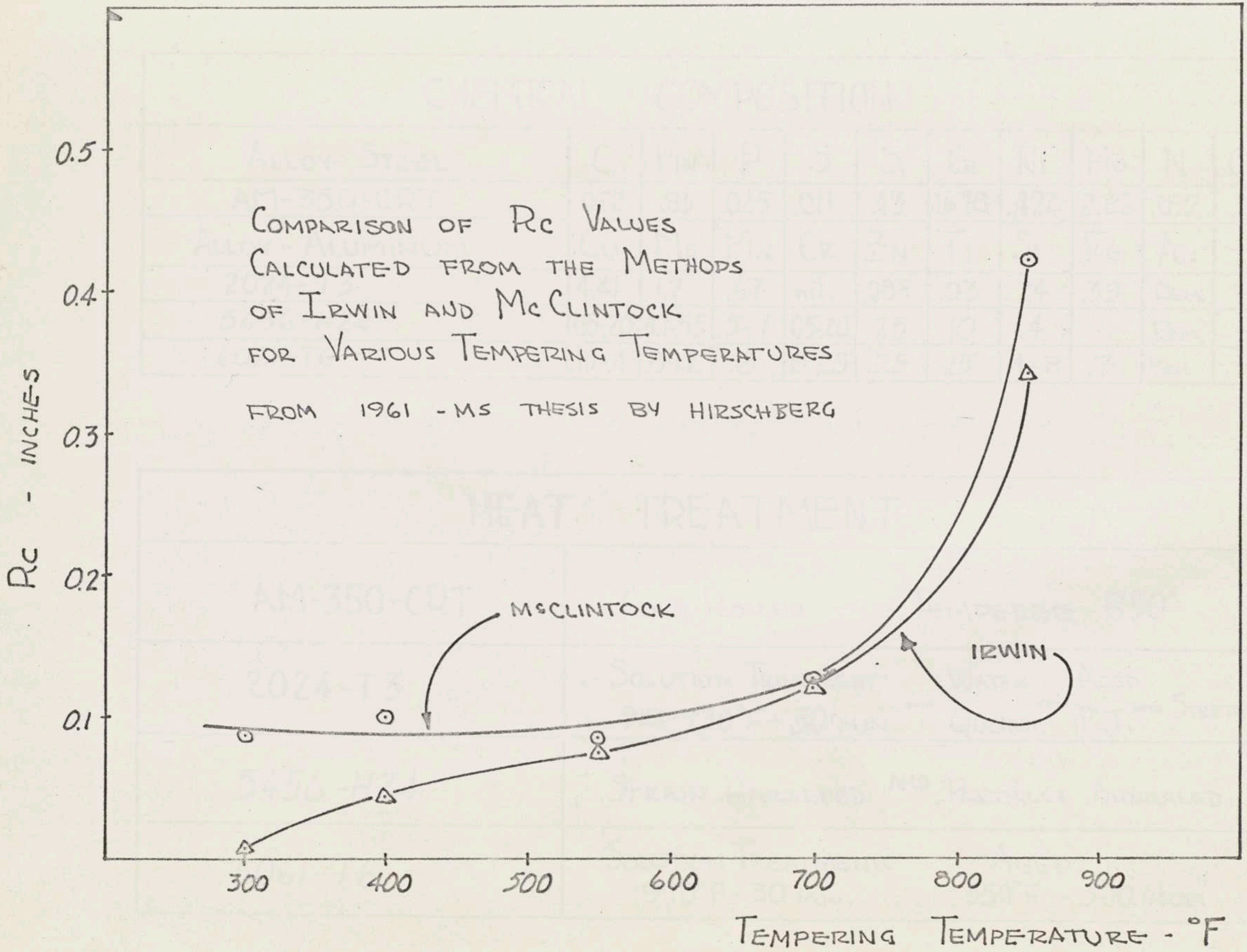
TABULATION OF R_c VALUES

ALLOY	THICKNESS (inches)	(1) R_c (inches)	(2) R_c (inches)	SOURCE OF DATA For (1)	SOURCE OF MATERIAL For (2)
2024-T3	.050	1.40*	———	(13)	———
bare	.050	.86*	———	(13)	———
bare	.050	.56	———	(13)	———
bare	.064	.73	1.71	(9)	A
bare	.064	.73	3.78	(9)	R
2024-T3	.040	.95	———	(10)	———
clad	.050	.58	1.45	(11)	R
clad	.063	.77	2.48	(9)	A
clad	.063	1.24*	3.14	(10)	R
5456-H24	.064	.81	1.61	(9)	A
6061-T6	.050	1.13	1.81	(11)	R
bare	.063	2.08	2.98	(9)	A
bare	.063	2.08	6.55	(9)	R
AM350(L)	.0205	.57	.53	(8)	Boeing
AM350(L)	.0205	1.15*	———	(8)	———

Notes: (1) based on data from large notched panels.
(2) based on data from specimens (1) and (2).

all specimens transverse to grain direction unless marked (L)

* indicates specimen stiffened to prevent buckling.


COMPARISON OF Rc VALUES
CALCULATED FROM THE METHODS
OF IRWIN AND McCLINTOCK
FOR VARIOUS TEMPERING TEMPERATURES
FROM 1961 - MS THESIS BY HIRSCHBERG
McCLINTOCK
IRWIN
Rc - INCHES
0.1
0.2
0.3
0.4
0.5
300
400
500
600
700
800
900
TEMPERING TEMPERATURE - °F

GRAPH 2

CHEMICAL COMPOSITION

Alloy- Steel	C	Mn	P	S	Si	Cr	Ni	Mo	N	Cu
AM-350-CRT	.092	.83	.025	.011	.43	16.78	.424	2.82	.092	.14
Alloy - Aluminum	**Cu**	**Mg**	**Mn**	**Cr**	**Zn**	**Ti**	**Si**	**Fe**	**Al**	—
2024-T3	4.41	1.2	.67	nil.	.085	.03	.24	.39	Rem.	—
5456-H24	.05-.20	4.7-5.5	.5-.1	.05-.20	.25	.10	.4 +	·	Rem.	—
6061-T6	.15-.4	.8-1.2	.15	.15-.35	.25	.15	.4-.8	.7	Rem	—

HEAT TREATMENT

AM-350-CRT	Cold Rolled - Tempered - 850°
2024-T3	Solution Treatment: 910-930°F - 30 min. — Water Quench — Aged R.T. — Stretched
5456-H24	Strain Hardened and Partially Annealed
6061-T6	Solution Treatment: 970°F - 30 min. Aged: 350°F - 5-10 hours

Table 5

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