June 5, 1972

Mr. E. F. Hogan
Director, Division Relations
Rocketdyne
North American Rockwell
6633 Canoga Avenue
Canoga Park, California 91304

Dear Mr. Hogan:

On May 31, 1972, you submitted copies of a paper, based on work performed under contract NAS7-506, for review prior to publication. Mr. R. S. Weiner, Technical Monitor of that contract, has reviewed the paper and has found it is suitable for open publication. I have discussed the paper with Mr. Frank Compitello, of NASA Headquarters, and he has authorized the open publication of information obtained under NASA contract NAS7-506.

Sincerely,

G. E. Nitzberg
Technical Assistant to Deputy Director

bcc: R. S. Weiner, JPL, Sec. 384 w/cy of Rocketdyne ltr. 5-72-47 and technical paper by H. Chu and W. Unterberg
F. E. Compitello, NASA Hqrs., Code RH, w/cy of Rocketdyne ltr. 5-72-47 and technical paper by H. Chu and W. Unterberg
J. M. Watson, NASA Hqrs., Code KSI, w/cy of Rocketdyne ltr. 5-72-47 and technical paper by H. Chu and W. Unterberg
31 May 1972

Mr. Gerald E. Nitzberg
Mail Stop 200-14
National Aeronautics and Space Administration
Ames Research Center
Moffett Field, California 94035

Dear Mr. Nitzberg:

Enclosed for your review prior to presentation and publication is the technical paper entitled "Life Prediction of Expulsion Bladders through Fatigue Test and Fold Strain Analysis" which we are proposing for presentation at the NATO Conference on Reliability Testing and Reliability Evaluation, to be held September 4-8, 1972 at The Hague, Netherlands.

Work described in this paper was performed under contract NAS7-506, for which R. S. Weiner at the Jet Propulsion Laboratory in Pasadena, is the technical contract monitor. No NASA funds will be used in connection with the travel expenses incurred in the presentation of this paper.

Since the deadline for submission of papers for the above meeting is June 1, 1972 we would appreciate receiving your approval as soon thereafter as possible.

Although the information contained in this paper is deemed to be unclassified, we are providing it with physical security protection equivalent to classified material until a determination regarding its open publication has been received.

Sincerely,

E. F. Hogan
Director
Division Relations

Encl. (1) Five copies: Life Prediction of Expulsion Bladders through Fatigue Test and Fold Strain Analysis, Walter Unterberg

cc: R. S. Weiner, J L (with enclosure)
LIFE PREDICTION OF EXPULSION BLADDERS THROUGH FATIGUE TEST AND FOLD STRAIN ANALYSIS

by

Hugh N. Chu
Walter Unterberg

Rocketdyne
A Division of North American Rockwell Corporation
Canoga Park, California

May 1972

To be Presented at the
NATO Conference on
Reliability Testing and Reliability Evaluation
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LIFE PREDICTION OF EXPULSION BLADDERS THROUGH FATIGUE TEST AND FOLD STRAIN ANALYSIS*

by

Hugh N. Chu** and Walter Unterberg

Rocketdyne/North American Rockwell Corporation
Canoga Park, California 91304 USA

INTRODUCTION

To ensure reliable restarts of spacecraft liquid rockets at zero gravity, bladder systems are often used for the positive expulsion of propellants into the engines. Usually, the propellant is contained in a thin-walled bladder that lines the propellant tank. For propellant expulsion, a pressurant gas is introduced between the tank wall and the thin bladder that collapses, flexes, and folds. The bladder must sustain such contortions without failure and also be impermeable to propellants and pressurants under various chemical and environmental conditions. In preflight tests, the bladder is subjected to a number of collapse/inflation cycles to ensure its integrity during its final operational collapse when expelling propellant in flight. The present paper is mainly concerned with the problem of systematically incorporating the mechanical failure aspect into design; but it also takes the impermeability aspect into consideration.

The work here reported was sponsored by the National Aeronautics and Space Administration under Contract NAS7-506, with R. S. Weiner as Technical Manager. A more extensive and detailed account is contained in Rocketdyne Report R-7391, which includes digital computer programs for all cycle life predictions and is available from the authors.

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*Work performed under National Aeronautics and Space Administration Contract NAS7-506

**Present Address: Lawrence Livermore Laboratory, University of California, Livermore, California 94550 USA
1. FOLD STRAIN ANALYSES

1.1 NOMENCLATURE AND GEOMETRIC PARAMETERS OF FOLDS

In the nomenclature of folds (Fig. 1), the words "primary" and "secondary" refer to time sequence. In an incomplete double fold, the secondary fold does not cross, but rather stops at, the primary fold. In the double fold, "a" refers to the inside fold radius of the secondary fold because only the specification of the secondary fold radius characterizes the double fold.

The definitions introduced above, together with their specifying geometric parameters, are precise (without being restrictive) and complete (yet simple). This is so because of two facts: (1) $h/a$ determines the stress and strain at the double fold regardless of the angle made by the secondary and primary fold lines and (2) folds cause only a local stress or strain that decreases rapidly with distance from the location of any particular fold. Thus, complicated bladder deformations can be regarded as isolated assemblies of individual folds. The many fold shapes commonly called creases, wrinkles, points, etc., are now replaced with technically precise terms.

1.2 SIMPLE FOLDS IN METALS AND PLASTICS

For simple folds, the true strain is defined as

$$
\varepsilon = \text{true strain} = \ln (1 + e)
$$

(1)

where

$$
e = \text{nominal strain} = -1/[1+2(a/h)]
$$

(2)

The minus sign refers to the inside fold surface. The inside fold surface strain (compressive) is always larger than the outside fold surface strain (tensile) in absolute value according to Eq. 1. This is consistent with the experimental fact that in folding/unfolding a sheet, the failure always starts at the inside surface of the fold. For large strains, such as those encountered in folds, the elastic part of the strain is negligible and therefore the strains defined in Eq. 1 and 2 may be taken as the plastic strains. The relation between the applied pressure differential, $p$, and the geometric parameter, $h/a$, was determined experimentally for an aluminum sheet. Figure 2 shows the experimental setup and Fig. 3 the data.
which may be represented by

\[ h/a = 3.2 \left( p/\sigma_o \right)^{1/3} \]  

(3)

where \( \sigma_o \) is the yield stress of the material. This relation was assumed to apply to all metals and plastics. Equations 1, 2, and 3 relate the applied pressure differential to the true strain via \( h/a \).

1.3 DOUBLE FOLDS IN METALS AND PLASTICS

An expression will be derived for the plastic strain due to double folds. This requires evaluation of anticlastic curvature effects and the shear strains due to bending and twist. From the treatment of Fung and Wittrick (Ref. 1), bending of a wide sheet (thickness \( h \)) to a radius of curvature gives rise to a region of anticlastic curvature along the sheet edge with end slope

\[ \nu \left( h/\rho \right)^{1/2} / \left[ 3 \left( 1 - \nu^2 \right) \right]^{1/4} \]  

(4)

where \( \nu \) is the Poisson ratio that equals 1/2 for plastic deformation. For a complete unconstrained fold (fold radius \( h/2 \)), the anticlastic curvatures of the inner \((1/\rho_i)\) and outer \((1/\rho_o)\) sheets vary, here by a factor of 2. In an actual fold for which the edges are joined (Fig. 4), a compromise is achieved between the two edges to preserve material continuity. This compromise results in a net twist, \( \theta \), of the end piece which, before the start of the secondary fold, may be pictured as a beam with a semicircular cross section. This twist was estimated to be the average of the end slopes of the two sheets taken separately, i.e.,

\[ \theta = \frac{\nu}{2 \left[ 3 \left( 1 - \nu^2 \right) \right]^{1/4}} \left[ \left( \frac{h}{\rho_i} \right)^{1/2} + \left( \frac{h}{\rho_o} \right)^{1/2} \right] \]  

(5)

Structural consideration of Fig. 4 indicates that the end piece is subject not only to twisting but also to bending as a beam. The plastic deformation tends to concentrate in a semicircular ring at the section of maximum transverse shear, SPRT (Fig. 4), which is the root of the primary fold. The normal strains suppressed at P and R involve values of \( a = \rho_o - 0.5 \ h \) and \( a = \rho_i - 0.5 \ h \), respectively. Applying Eq. 2, these strains are \(- h/2\rho_o\) and \(+ h/2\rho_i\), and they act at distance \( h \), approximately, from SPRT.

On the other hand, the shear strain due to bending, \( \gamma_b \), changes from zero to its maximum value approximately through the length of a quarter
circle, \((\pi/2)(\rho_1 + 0.5h)\). Therefore, 
\[ h(0.5h/\rho_o + 0.5h/\rho_1) = \gamma_b(\pi/2) \]
\((\rho_o + 0.5h)\) from which
\[ \gamma_s = \frac{1}{\pi} \left( \frac{h}{\rho_o} + \frac{h}{\rho_1} \right) \left( \frac{\rho_1}{h} + \frac{1}{2} \right) \]  
(6)

The shear stresses at the root of the primary fold due to twist may be assumed to be the same as those in a circular shaft (Fig. 4) with a similarly situated radial slit. For the elastic case, Shepherd (Ref. 2) found that because of twist the two sides of the slit are displaced with respect to each other, perpendicular to the plane of the paper in Fig. 4b. The maximum relative displacement is at the lip of the slit and is about 3.9 \(\theta h\), where \(\theta\) is the angle of twist of the end piece and \(h\) is the radius of the circular shaft, i.e., the sheet thickness. It was estimated that 80\% of this displacement may be realized at point Q in Fig. 4b by plastic slip. This slip should be accomplished essentially within a height, \(\delta\), of the width of the slit, or approximately twice the root radius of the primary fold. Then the shear strain due to twist, \(\gamma_t\), may be estimated by the formula
\[ \gamma_t = 3.9 \theta h \times 0.8/\delta = 3.12 \theta h/\delta, \]  
where \(\theta\) is given by Eq. 5  
(7)

In an actual bladder, the sheet material has already suffered some damage before the start of the secondary fold. There does not appear to be any simple way to account for the effect of the residual strain. A expedient way is to regard the double fold as two simple folds because in one cycle of the double fold the tangential surface strain of the primary fold actually goes through two cycles. In other words, life is reduced by one-half and this is equivalent to saying that strain is increased by a factor of \(\sqrt{2}\) (Eq. 10). Thus, a tentative way to account for the effect of the residual strain is to multiply the total strain \((\gamma_b + \gamma_t)\) calculated from Eq. 6 and 7 by a factor of \(\sqrt{2}\). From Eq. 5, 6, and 7, with \(\nu = 1/2\), we obtain the total strain as
\[ \varepsilon_p = \sqrt{2} \left[ \left( \frac{1}{\pi} \left( \frac{h}{\rho_o} + \frac{h}{\rho_1} \right) \left( \frac{\rho_1}{h} + \frac{1}{2} \right) \right) + 0.6 \left( \frac{h}{\rho_o} + \frac{h}{\rho_1} \right)(h/\delta) \right] \]  
(8)

where \(\rho_o, \rho_1\) = radius of curvature of the outer and inner sheet of the double fold, respectively, and \(\delta\) = twice inside radius of primary fold.

For the complete double fold, \(\rho_1 = a + 0.5h\) and \(\rho_o = a + 1.5h\), where \(a\) is the inside fold radius of the secondary fold. For the incomplete fold,
\( \rho_0 \) can vary from \( a + 1.5 \ h \) to infinity, whereas \( \rho_1 \) is still \( a + 0.5 \ h \). In Fig. 5, \( \varepsilon_p \) vs \( a/h \) is plotted for various values of \( \delta/h \) for the complete and most incomplete (\( \rho_0 = \infty \)) double folds. For all other incomplete double folds, the curves fall between those shown. It is seen that the maximum plastic strain that can develop in a double fold far exceeds the fracture ductility of typical ductile metals. Therefore, a surface crack can be expected already after one folding/unfolding cycle of a double fold. Although this does not necessarily imply that the sheet has cracked through its thickness, good design avoids a surface crack that can propagate rapidly through the sheet thickness. It follows that in bladder design all double folds (complete and incomplete) should be avoided.

For the double fold, the pressure acting on the outer and the inner surfaces of the secondary fold is the same because both surfaces are actually the same side of the sheet. The inside fold radius of the secondary fold is thus not a function of pressure.

1.4 SIMPLE AND DOUBLE FOLDS IN ELASTOMERS

Fold strains in elastomers are large elastic deformations characterized by the extension ratio \( \lambda \), defined as the deformed length divided by the original length. In uniaxial extension \( \lambda = 1 + \varepsilon \). Folds in a rubber sheet are complex deformations and rigorous mathematical solutions are rather unlikely (Ref. 3). Now contrary to metals and plastics that fail due to a cumulative loss of ductility which is a maximum at the inside fold, rubbers fail by extension, which is a maximum at the outside fold. The fatigue of rubber is by the growth of an initial flaw under repeated extension, therefore one needs only to determine the maximum extensions at the outside fold. The experimental results of Ref. 4 were obtained by printing a grid on a rubber sheet, bending it and measuring the change in grid length. The maximum \( \lambda \) in a simple fold was about 1.5. The maximum \( \lambda \) in a complete double fold was about 1.9, in a direction parallel to the primary fold line. These numbers will be subsequently used for cycle life calculations.
2. CYCLE LIFE: METALS AND PLASTICS

2.1 BASIS FOR CYCLE LIFE PREDICTION

The structural deterioration of expulsion bladders under alternating collapse and inflation is an example of low-cycle fatigue. This is characterized by high internal stresses, coarse slip, macro-sized plastic flow, and relatively small number of cycles to failure.

By studying a large number of experimental low-cycle fatigue data, Coffin (Ref. 5) and Manson (Ref. 6) independently proposed the relation:

\[ \varepsilon_p N^k = C \]  

(9)

as the best fit to the data. Here, \( k \) and \( C \) are material constants. Coffin suggested that \( k = 1/2 \) and \( C = \varepsilon_f/2 \) for all metals, where \( \varepsilon_f \) is fracture ductility, i.e., the true strain at fracture. Later, Martin (Ref. 7) re-studied the data and found that \( C = \varepsilon_f/\sqrt{2} \) gave a better fit. Thus the following equation will be adopted:

\[ \varepsilon_p \sqrt{N} = \varepsilon_f/\sqrt{2} \]  

(10)

Exceptions to Eq. 10 will be made whenever the data points for a particular material are so scatter-free as to warrant the use of a \( k \) value other than 1/2.

2.2 CYCLE LIFE DATA

Typical metal fatigue curves are shown in Fig. 6 through 9 for aluminum 1100, stainless steel 347, 6Al-4V titanium, and pure gold, respectively. These curves are derived in three different ways, in decreasing order of confidence: Fig. 6 and 8 are based on the best exponential of direct fatigue tests (Ref. 8 and 9), the slope in Fig. 8 being -0.74 rather than -0.50. Figure 7 is calculated from Eq. 10 with true strain ductility data at various temperatures from Ref. 10. Figure 9 uses Eq. 10 with estimated ductilities from unpublished tensile test data (Ref. 11). For this case, it was assumed that the \( \varepsilon_f \) was identical for aluminum 1100 and pure gold because both are face-centered-cubic metals, subject to coarse slip under low-cycle fatigue conditions.

Cycle life data for plastics are scarce, and of questionable suitability for the present purpose. Therefore, it was decided to determine
the needed data directly by test. A typical test setup is shown in Fig. 10. The tests were run both at room temperature and at 200 F. The results are plotted in Fig. 11 and 12. The data points can be correlated by Eq. 9 with the following constants:

<table>
<thead>
<tr>
<th>Plastic</th>
<th>Room Temperature</th>
<th>200 F</th>
</tr>
</thead>
<tbody>
<tr>
<td>Teflon TFE</td>
<td>$k = 0.203$, $C = 1.25$</td>
<td>$k = 0.433$, $C = 2$</td>
</tr>
<tr>
<td>Teflon FEP</td>
<td>$k = 0.084$, $C = 1.0$</td>
<td>$k = 0.044$, $C = 1$</td>
</tr>
</tbody>
</table>

2.3 CYCLE LIFE PREDICTION

For simple folds with specified $p$, $\sigma_0$ is found from tabulated properties for the given material, $(h/a)$ is found from Eq. 3, and then $\epsilon_p$ from Eq. 1 and 2. Lastly, $N$ is read off the proper fatigue curve (e.g., Fig. 6-9, 11, 12) for the specified ambient temperature.

Cycle life is not a serious problem for bladders with simple folds only. What causes most failures in bladders is double folds. From Eq. 10, $N = 1$ if $\sqrt{2} \epsilon_p \geq \epsilon_f$. Referring back to Fig. 5, $\epsilon_p \geq 2$ for practical values of $\delta/h$ (probably less than 0.1) and practical values of $a/h$ (probably less than 1.5). That is, $\sqrt{2} \epsilon_p > 2.828$. This is greater than the fracture ductilities of all the metals or plastics considered. Hence, there is no need to calculate the cycle life $N$ for bladders with double folds. $N = 1$ for all metals and plastics according to the definition of failure used in this study--the appearance of a surface crack.

3. CYCLE LIFE: ELASTOMERS

3.1 BASIS FOR CYCLE LIFE PREDICTION

During the period 1953 to 1964, Gent, Greensmith, Lake, Lindley, Rivlir, and Thomas (Ref. 12 through 22) published a series of papers that established the cut-growth theory of rubber fatigue. This theory will be the basis for cycle life prediction of elastomeric bladders in the generalized form

$$N = G(2K)^{-m} (m-1)^{-1} C_0^{1-m}$$

where $N$ = the cycle life of the rubber in question; $G$, $m$ = material constants to be determined by cut-growth tests; $W$ = stored energy function to be determined by static tests, $k$ = a factor dependent on the extension ratio, may be taken to be 2 if the extension ratio is less than 5; and $C_0$ = initial flaw length, usually taken to be 0.002 inch. The rubbers
considered in this paper are assumed to be Mooney-Rivlin materials for which

\[ W = C_1(I_1 - 3) + C_2(I_2 - 3) \]  \hspace{1cm} (12)

where \( I_1 = \lambda_1^2 + \lambda_2^2 + \lambda_3^2; \ 1 = \lambda_1^{-1} + \lambda_2^{-2} + \lambda_3^{-2}; \) and \( \lambda_1\lambda_2\lambda_3 = 1; \)  \hspace{1cm} (13)

\( \lambda_1, \lambda_2, \lambda_3 = \) principal extension ratios; and \( C_1, C_2 = \) material constants.

The material constants may be determined from static tests on a thin strip of the typical dimensions of Fig. 13A. If \( \lambda = \lambda_1 \) designates the longitudinal extension ratio, then from Eq. 13, \( \lambda_2 = \lambda_3 = 1/\sqrt{\lambda}. \) Substitution into Eq. 12 and differentiation yields

\[ \text{Stress} = F/A_0 = \partial W/\partial \lambda = 2(\lambda - \lambda^{-2})(C_1 + C_2\lambda^{-1}) \]  \hspace{1cm} (14)

where \( F = \) applied tensile load and \( A_0 = \) unstressed cross-sectional area of strip. Equation 14 is a straight line on an \( F/[2A_0(\lambda - \lambda^{-2})] vs \lambda^{-1} \) plot in which \( C_1 \) becomes the ordinate at \( \lambda = \infty \) (or \( \lambda^{-1} = 0 \)), whereas \( C_1 + C_2 \) is the ordinate at \( \lambda = 1. \) In reality, \( \lambda = 1 \) and \( \lambda = \infty \) are not directly obtainable experimental points. However, \( \lambda \) values in between are obtainable and the values at \( \lambda = 1 \) and \( \lambda = \infty \) are then obtainable by simple extrapolation. However, if the specimen of Fig. 13B (with the side cut) is stretched, the tearing energy associated with the cut \( T = 2kW \) increases because \( W \) increases. When \( T = T_c, \) the catastrophic tearing energy, the cut suddenly begins to grow in length. Now if the specimen is stretched cyclically by an amount corresponding to a \( T < T_c, \) the cut length \( c \) is also found to grow in length with \( n, \) the number of cycles. That is, dynamic cut growth can occur at an extension ratio which is less than that for the static cut growth. If the specimen of Fig. 13B is stretched repeatedly to a fixed \( \lambda, \) the following relation exists for most rubbers:

\[ \frac{dc}{dn} = \frac{T}{G} \text{ or } \log(\frac{dc}{dn}) = m \log T - \log G \]  \hspace{1cm} (15)

Thus, if \( dc/dn \) is plotted vs \( T \) on log-log paper, a straight line results. The slope of this straight line is \( m, \) and its vertical intercept is \( \log G. \) Hence, the constants \( m \) and \( G \) can be determined.

3.2 CYCLE LIFE DATA

Figure 14 shows the apparatus used in the cut-growth experiments. By keeping the arm stationary, however, it was also utilized to determine the stored energy functions. The specimens were dimensioned as in Fig. 13A and 13B. Three elastomers were tested: EPR-PRC (ethylene-propylene
copolymers from Parker Rubber Company), EPR-RMD (same, but from Reaction Motors Division of Thiokol Chemical Corporation), and NR-RMD (carboxy-nitroso terpolymer rubber from Reaction Motors). The rubbers were of limited extensibility, with a maximum static fracture \( \lambda = 3.6 \). The cut-growth tests were carried out up to \( \lambda = 1.6 \), close to the failure point with the cut present. Typical data taken for the stored energy function are plotted on Fig. 15 confirming the Mooney-Rivlin behavior up to a limiting \( \lambda = \lambda_{\text{lim}} \). Typical cut-growth data are shown in Fig. 16.

The numerical results follow:

\[
W, \text{ Eq. 12} \quad C_1, C_2, \lambda_{\text{lim}}, G, m
\]

<table>
<thead>
<tr>
<th>Elastomer</th>
<th>( C_1 ) (in.-lb/in.²)</th>
<th>( C_2 ) (in.-lb/in.²)</th>
<th>( \lambda_{\text{lim}} )</th>
<th>( G ) (x 10⁶)</th>
<th>( m )</th>
</tr>
</thead>
<tbody>
<tr>
<td>EPR-PRC</td>
<td>30</td>
<td>174</td>
<td>1.6</td>
<td>5.25 x 10⁶</td>
<td>3.55</td>
</tr>
<tr>
<td>EPR-RMD</td>
<td>10</td>
<td>83</td>
<td>1.6</td>
<td>5.96 x 10⁷</td>
<td>3.3</td>
</tr>
<tr>
<td>NR-RMD</td>
<td>38</td>
<td>110</td>
<td>3.6</td>
<td>2.74 x 10⁷</td>
<td>2.74</td>
</tr>
</tbody>
</table>

### 3. CYCLE LIFE PREDICTION

The values of \( N \) for each elastomer are now found from Eq. 11, with \( \lambda = 1.5 \) for simple folds or \( \lambda = 1.9 \) for double folds; \( k = 2 \); \( C_0 = 0.002 \) inch; and \( W[f(C_1, C_2)] \), \( G \) and \( m \) as determined above:

<table>
<thead>
<tr>
<th>Elastomer</th>
<th>Simple Fold</th>
<th>Double Fold</th>
</tr>
</thead>
<tbody>
<tr>
<td>EPR-PRC</td>
<td>11,000</td>
<td>422</td>
</tr>
<tr>
<td>EPR-RMD</td>
<td>1,760,000</td>
<td>89,200</td>
</tr>
<tr>
<td>NR-RMD</td>
<td>149,000</td>
<td>11,500</td>
</tr>
</tbody>
</table>

In view of the scatter of the data, it is prudent to use some factors of safety. Based on design practices in use with pressure vessels subjected to cyclic loading, the following factors of safety (FS) were selected:

- \( \text{FS} = 20 \) for \( N > 10^4 \) cycles
- 10 for \( 10^4 > N > 300 \) cycles
- 5 for \( N < 300 \) cycles

The allowable \( N \) then is the calculated \( N \) divided by FS. It is recommended that when the calculated \( N \) falls below 100 or so, the particular elastomer be abandoned.
SUMMARY AND CONCLUSIONS

Individual local folds in collapsing bladders can be classified into single and double folds, specifiable by simply nondimensional geometrical parameters that define directly the maximum strains at these folds. From these maximum strains the number of collapse/inflation cycles to failure can be obtained from low-cycle fatigue data experimentally determined for the bladder material of interest.

Cycle life data are presented in terms of true maximum strain for four metals (300-series stainless steel, 1100-series aluminum, gold, and a titanium alloy), two plastics (teflon TFE and FEP), and two elastomers (ethylene-propylene copolymers rubber and carboxy-nitroso terpolymer rubber). The Coffin-Manson fatigue theory was applied for metals and plastics, and the cut-growth fatigue theory for elastomers. Data for gold, plastics, and elastomers are new, based on measurements at room and elevated temperatures.

It was found that double folds give rise to far severer folding strains than do simple folds. On a bladder whose collapse is uncontrolled there occur numerous double folds so that the occurrence of at least one most severe double fold approaches certainty. Therefore, it is only necessary to ascertain the maximum strain of the most severe double fold which can develop under the existing conditions for the material. The result was that, except for the elastomers, all bladder materials develop surface cracks due to double folds after only one cycle. Surprisingly, plastics crack at a lower maximum surface strain than do metals. Thus, ultimate failure is controlled by the propagation of the surface crack through the bladder sheet thickness during the subsequent cycles. Since the speed of crack propagation increases with the speed of sound in a material, the metals are the least satisfactory. This leads to the conclusion that metals—which are best for permeation resistance—are worst for fatigue resistance, and vice versa for elastomers. The intermediate plastics are found to be unsatisfactory for both permeation resistance and fatigue resistance for missions of extended duration.
REFERENCES

11. Personal communication from N. J. Hoffman, Rocketdyne, a Division of North American Rockwell Corporation, Canoga Park, California, 1967.
Figure 1. Fold Nomenclature

Figure 2. Compression Test Setup for Simple Folds

Figure 3. Curvature of Simple Fold vs Applied Pressure
Figure 4. Double Fold and Coordinate Axes

Figure 5. Maximum Plastic Strain in Double Folds
Figure 6. 1100 Aluminum
(Ref. 8)

Figure 7. 347 Stainless Steel
(Data from Ref. 10)

Figure 8. 6Al-4V Titanium
(Ref. 9)

Figure 9. Pure Gold
(Data from Ref. 11)

Low-Cycle Fatigue Curves for Metals
Figure 10. Cycle Life Test for Plastics

Figure 11. Teflon TFE

Figure 12. Teflon TEP

Low Cycle Fatigue Curves for Plastics
Figure 13. Elastomer Test Specimens

Figure 14. Fatigue Testing Apparatus for Elastomers

Figure 15. Determination of Constants in Stored Energy Function (NR-RMD Elastomer)

Figure 16. Determination of Out-Growth Constants (EPR-PRC Elastomer)