950 2974

University of California
Department of Mechanical Engineering
Davis, California 95616

Final Report No. 8

August 1982

RESEARCH ON THE "HYLIFE" LIQUID-FIRST-WALL CONCEPT FOR
FUTURE LASER-FUSION REACTORS:††

LIQUID JET IMPACT EXPERIMENTS[§] / <

DISCLAIMER

This report was prepared in an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise, does not necessarily constitute or imply its endorsement, recommendation, or approval by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

Principal Investigator: Professor Myron A. Hoffman

Research Assistant: A. Rene Raffray

NOTICE

PORTIONS OF THIS REPORT ARE ILLEGIBLE. IT
has been reproduced from the best available
copy to permit the broadest possible avail-
ability.

Work performed on Intramural Purchase Order No. 4794109 from the
Lawrence Livermore National Laboratory, Livermore, California 94550

FOB

CONTENTS

1. Motivation
2. Review of Previous Work
3. Scaling Laws
4. Near-Universal Transient Curves
5. Experimental Apparatus
6. Results for Single Rods
7. Results for Rod Arrays
8. Application to HYLIFE
9. Conclusions and Recommendations

LIQUID JET IMPACT EXPERIMENTS

M. A. Hoffman and A. R. Raffray

I. MOTIVATION

There are several important problem-areas related to the efforts to extend the lifetime of the vacuum vessel for the HYLIFE laser fusion reactor concept shown on Figure 1 [1]. One of these is the protection of the vacuum vessel from excessive erosion due to the local impact stresses caused by liquid lithium droplets and globules. These liquid droplets and globules are produced during the disassembly of the liquid lithium jets after the fusion microexplosion.

One possible method for reducing the hoop stress (created by the impact of the liquid on the vacuum vessel) proposed by the Lawrence Livermore National Laboratory (LLNL) engineers is to set up an array of bars in front of the vacuum vessel. These bars are to be designed to absorb a large fraction of the liquid momentum as the liquid passes through the bars toward the vacuum vessel. The liquid drag force on the bars must, in turn, be transmitted to the bar supports. It is thus very likely that the bars themselves will have a shorter lifetime than the vacuum vessel due to the stresses induced by both the liquid impact and the resulting vibration of the bars. Consequently, the bars may have to be considered as a type of sacrificial first wall shield which would have to be removed and replaced at the end of its lifetime, but which would extend the vacuum vessel lifetime.

The goal of this initial scoping study was to evaluate the transient and steady state drag of a single bar and of some selected arrays of bars and to determine the momentum removed from impacting liquid slugs. In order to achieve this aim, use has been made of both the published literature and experimental data obtained from a small-scale experimental apparatus. In Section 2, we review the relevant published papers. Then in Section 3, the implications of two possible scaling laws for use in designing the small-scale experiment are discussed. The use of near-universal curves to evaluate the momentum removed during the initial transient period is described in Section 4. The small-scale apparatus used to obtain steady-state drag data is described in Section 5, while the results are presented in Section 6 for single rods and in Section 7 for two specific rod arrays. Finally these results are applied to the HYLIFE fusion reactor in Section 8.

2. REVIEW OF PREVIOUS WORK

The impact drag on circular cylinders has been of interest in many fields including aerodynamics, design of off-shore structures, and accident analysis of nuclear powerplants. One key experimental study was the one sponsored by EPRI (the Electric Power Research Institute) at Developmental Sciences, Inc. (DSI) to investigate the pool swell impact forces from a postulated LOCA (loss of coolant-accident) [2].

The test facility described in the EPRI report was constructed to drive simple cylindrical models vertically downward at constant speeds from 5 to 24 ft/sec (1.5 to 7.3 m/s) into a flat pool of water. The small and large test cylinders were 210 and 432 mm in diameter, respectively,

and were instrumented with miniature pressure transducers, accelerometers, and strain gauges. The average pressure and impulse histories were electronically calculated. Typical examples of the average pressure and impulse histories obtained are shown on Figure 2a.

From this figure, it is interesting to note the initial rapid pressure rise to a peak of about 6 times the dynamic pressure, $q = \rho V_0^2/2$, based on the impact velocity, V_0 . The average impulse history shown on the figures is the time integral of the average pressure history. After several milliseconds (i.e., when the cylinder is submerged to about a third of its radius), the average pressure appears to be approaching an asymptotic value on the order of about half the dynamic pressure, $q/2$. The typical range of values obtained for the peak pressure for the various runs presented in Ref. [2] was from about $5q$ to about $8q$. The implications of these results will be discussed in more detail in Section 4.

EPRI also funded four different groups to perform analytical studies to attempt to model the fluid-structure interaction [3]. Of the four solution methods investigated, the incompressible Eulerian fluid method (using the SOLA-SURF code) of the Los Alamos National Laboratory (LANL) appeared to give the best results.

A typical LANL result from Ref. [3] is shown in Figure 3 where it can be seen that the agreement with the experimental results is very good. However, no attempt seems to have been made in any of the analytical studies to develop simple scaling laws or to verify proposed scaling laws.

The initial impact phase for a cylinder impacting a very large body of liquid is shown in Figure 4 [3]. This figure illustrates how the flow patterns develop around the cylinder and illustrates the capabilities of

Figure 2a Comparison of Scaled Loads from Both Rigid Models (16.9 fps) (from Ref. 2)

Figure 2b Comparison of Froude-scaled Loads from Both Rigid Models (from Ref. 2)

Figure 3 Comparison of the SOLA-SURF computer code calculation with typical DSI experimental results (from Ref. 3)

Figure 4 Typical computed flow patterns at a series of times after the impact of a cylinder with a large body of water (from Ref. 3)

the LANL computer simulation as well. Also shown in Ref. [3] are some flow patterns for large sheets of water of finite depth impacting a cylinder. The patterns are qualitatively much the same as in Figure 4.

Several useful papers related to wave load estimation on offshore structures were uncovered [4-7]. Dalton and Nash [4] attempted to measure wave slam on a horizontal cylinder. They found initial peak wave slam coefficients, C_s , ranging from about 1 to 4.5 for various test conditions. This coefficient is basically the same as the peak pressure divided by q ; i.e., it is defined in the same way as a conventional drag coefficient:

$$C_s \equiv F_s / (0.5 \rho V_0^2 A_p)$$

where F_s is the peak slam or impact force and A_p is the projected area of the cylinder normal to the plane of wave impact.

The force measurements were made using strain gauges mounted on biaxial flexural units. It should be noted that the initial slamming force measured depends crucially on the dynamic response of the member and the measurement system as well as on the nature of the wave profile. This presents serious problems in trying to interpret experiments of this type.

Kaplan and Silbert [5], Faltinsen et al., [6] and Sarpkaya [7] all tried to address these problems of predicting the slamming coefficient and interpreting experimental results. Refs. [5] and [6] develop quite similar analytical models for the transient wave impact forces. Faltinsen's model gave a theoretical C_s at the moment of impact of 3.1 compared to his experimental values of 4.1 to 6.4.

Sarpkaya [7] carried the analytical work further by considering the effects of an elastic member. He demonstrated that the dynamic response of the system was as important as the wave impact in determining the peak forces on the member and the transient rise time. Using accelerometers mounted inside rigid cylinders, he measured initial slamming coefficients of about $3.17 \pm .05$. This compares very well with his theoretical value of π . However, he notes that the maximum slamming force could be amplified by a factor of as much as 1.7 by the dynamic response of an elastic structure.

For the classical steady flow about a cylinder, Schlichting [8] gives the variation of C_D with Reynolds number for a fully immersed cylinder in an infinite fluid flow. These classical results for the drag coefficient shown in Figure 5 are useful for comparison with our present case of a cylinder being impacted by a finite-size jet. Brodetsky [9] has found a theoretical steady drag coefficient of about 0.5 for the case of a two-dimensional inviscid jet. When this 2-D jet impacts the cylinder, it splits or separates into two equal 2-D streams or jets. The experiments of Ref. [2] seem to be approaching steady state drag coefficients somewhat larger than 0.5. However, noise on the pressure signal makes it very difficult to evaluate the exact steady state asymptote (see Figure 2).

In a study directly related to this present research, Grumman Aerospace Corp. [10] did a preliminary design of a helical assembly of rods for use as a sacrificial first wall inside the vacuum vessel of an inertial confinement fusion reactor (see Figure 6). In their analytical study they assumed a peak transient pressure of $7q$. They then assumed that the pressure decayed linearly to the steady value (which was

Figure 5 Classical drag coefficient versus Reynolds number for a cylinder in an infinite (or very large confined) flow (from Ref. 8)

Fig. 6 Tapered HYLIFE with Helical Energy Absorbers & Nozzle Plate
(from Ref. 10)

dependent on the spacing between the helical rods and the rod diameter) in a characteristic time equal to the time for the flow to travel around a rod. In addition, they considered the effect of the lightly damped oscillations of the rods on the stresses in the rods.

It should be noted that none of the above studies was concerned with the very large initial Hugoniot pressure which occurs on the acoustic time scale [1b]. This is due in part because the momentum transferred during this very brief initial phase of the impact (on the order of microseconds) is usually negligible compared to the momentum removed in subsequent phases.

3. SCALING LAWS

Two possible hydrodynamic scaling laws are discussed in Ref. [2], Froude scaling and replica scaling. In the traditional Froude scaling, the Froude number, $F \equiv V_0^2/gR$, is maintained the same in the model test as in the "prototype" full-scale system. Here V_0 is the impact velocity and R is the cylinder radius.

Replica scaling requires that the pressures on the model be the same as on the prototype system. These two scaling laws lead to the scaling results shown in Table 1 (taken from Ref. 2). The scale factor, λ , is the ratio of the model diameter to the full-scale "prototype" diameter.

For conditions where gravity effects are small, replica scaling is an acceptable approach. This was shown to be valid for the EPRI-sponsored experiments [2] as illustrated by the good agreement between two experimental runs in Figure 2a using replica scaling. The replica scaling also appears to be a valid approximation for our experiments [11], although

TABLE 1* - SCALING RELATIONSHIPS

PARAMETER	PROTOTYPE	MODEL	
		REPLICA SCALING	FROUDE SCALING
<u>General</u>			
Dimension	L	λL	λL
Impact Velocity	V_0	V_0	$\sqrt{\lambda} V_0$
Froude Number	F	F/λ	F
Average Pressure	$\bar{P}$	$\bar{P}$	$\lambda \bar{P}$
Total Impulse	$\bar{I}$	$\bar{I}$	$\lambda^{3/2} \bar{I}$
Time	t	λt	$\sqrt{\lambda} t$
<u>Hydrodynamic</u>			
Density of Water	ρ_W	ρ_W	ρ_W
Pressure (local)	p	p	λp
Impulse (local)	I	λI	$\lambda^{3/2} I$
Gravity Constant	g	λg	g
<u>Structural</u>			
Density of structure	ρ_S	ρ_S	ρ_S
Modulus	E	E	λE
Displacement	w	λw	λw
Strain	ϵ	ϵ	ϵ
Stress	δ	δ	$\lambda \delta$
Strain rate	$\dot{\epsilon}$	$\dot{\epsilon} \lambda$	$\dot{\epsilon} \sqrt{\lambda}$
Frequency	ω	$\omega \lambda$	$\omega \sqrt{\lambda}$

*From Table A-1 of the EPRI Report [2]

Froude scaling could also be used as demonstrated in Ref. 2 by the good agreement between the small and large rigid cylinder experiments based on Froude scaling shown in Figure 2b. This had very serious implications for the design of our experimental apparatus. For our limited size water jets, convenient rod diameters for our tests, were on the order of 1 to 4

For the EPRI experiments with cylinder diameters up to 432 mm, the initial rise time to the peak pressure was less than 1 ms. Therefore, in our smaller scale experiments, the rise time was expected to be less than 10 μ s based on replica scaling. (Note that Froude scaling would still give rise times less than about 100 μ s).

Measuring the pressure response on such small time scales requires sophisticated equipment with high frequency response, such as the piezoelectric transducers used in the EPRI experiments. However, the pressure transducers need to be installed inside the cylinder itself for best results. Consequently, experiments on this small a time scale were considered to be beyond the scope of our program (and budget).

As an alternative to performing these transient measurements, we decided to see if the existing experimental results could be used to estimate the momentum removal during the transient period. This led to the idea of examining the existing experimental results in a non-dimensional form.

4. NEAR-UNIVERSAL TRANSIENT CURVES

It seems that the EPRI report and much of the literature about water/cylinder impact ([2], [6], and [7], for example) only show the results for specific cases in terms of specific units. We felt that it might be helpful to consider the use of non-dimensional time, pressure and impulse to see if the resulting non-dimensional pressure and impulse time-histories were universal.

To investigate this possibility, all the graphical results for the impact of rigid cylinders in water from the EPRI report [2], were used to obtain values of the pressure, P , and impulse per unit area, I_a , at various times, t , after impact. These were then non-dimensionalized by using the following expressions, with the dash, ' $'$ ', representing the non-dimensionalized variable:

$$t' = V_0 t / D$$

$$P' = P / (0.5 \rho V_0^2)$$

$$I_a' = I_a / (0.5 \rho V_0^2 \times D / V_0) = 2 I_a / \rho V_0 D$$

where V_0 = initial impact velocity

D = cylinder diameter = d_r , the rod diameter in our notation

ρ = liquid density

$$I_a(t) = \int_0^t \bar{p} \, dt$$

The non-dimensionalized results from Faltinsen et al.'s, experiments [6] on the impact of a cylinder forced at constant velocity in a water pool, were also used. (See Ref. [11] for the tabulated data.)

Figures 7 and 8 show the dimensionless pressure and impulse time-histories obtained from these two reports. As can be seen, the correlation in the case of the dimensionless pressure is relatively good up to about $t' = 0.15$. Then, there is a somewhat larger scatter of the data points. In the EPRI report [2], they state that P' approached a value somewhat greater than 0.5; however, noise on the pressure signal prevents us from making a more accurate estimate.

In the case of the dimensionless impulse, the scatter again tends to get larger with larger t' but in both cases the data points lie within a narrow enough region to suggest these near-universal dimensionless curves

Figure 7 Dimensionless pressure versus dimensionless time based on experimental data for the impact between cylinders and water in Refs. 2 and 6

Figure 8 Dimensionless impulse versus dimensionless time based on experimental data in Figure 7 from Ref. 2

might indeed be useful for estimating the momentum removed from the impacting liquid jets during the transient period. The estimation procedure will be discussed further in Section 8 in connection with the application to the HYLIFE fusion reactor.

5. EXPERIMENTAL APPARATUS

The experimental apparatus was a modification of the water jet apparatus shown in Figure 9 which was used in previous laser fusion experimental studies (e.g., see Ref. 12). The electromagnetic shaker system was not used in this study. Instead, a cantilever beam support and measurement system was designed for holding a single rod or array of rods. The rods were then placed in the path of the water jet. A sketch of this cantilever-beam system and the strain gauge measurement system is shown in Figure 10.

Two strain gauges, one bonded to each side of the beam, formed a Wheatstone bridge when connected to two dummy gauges in a strain-indicator box. Any applied force on the rod caused a voltage signal to be produced and sent via a frequency filter to a digital voltmeter. Two strain gauges were used on the cantilever beam primarily to provide temperature compensation as discussed in Ref. [11]. The system was calibrated using a set of weights placed at the nominal water jet impact point.

Two different water lines used: line 1 was attached to a small nozzle (12.52 mm exit diameter) and line 2 was attached to a larger nozzle (50.8 mm exit diameter). Each piping system had a venturi in the line and a corresponding manometer, with one side connected to the venturi throat and the other to the water line just upstream of the venturi. In both cases,

Figure 9 Apparatus used in various water jet experiments at UCD
(University of California, Davis)

Figure 10 Cantilever-beam and strain gauge measurement system used for steady-state drag measurements on single rods and rod arrays

the venturi and manometer were calibrated before being used for the experimental purposes, as described in Ref. [11].

In addition to the above steady-state drag measurement apparatus, transient drag measurements were attempted using a set of four strain gauges mounted on a specially designed octagonal nut (sometimes referred to as Moorehouse "rings"). The detailed design of the octagonal nut system for very high frequency response while still providing adequately large signal output levels is described in Ref. [11]. The final octagonal nut design had a natural frequency of about 3.6 kHz without any rods attached. However, the maximum natural frequency of the system with rods attached was predicted to be only about 700 Hz; this was verified experimentally. Consequently, this system was used only for the initial steady-state measurements while the more sensitive cantilever-beam apparatus shown in figure 10 was used for the definitive steady-state experiments.

6. RESULTS FOR SINGLE RODS

Figure 11 shows the steady-state drag coefficient obtained for a variety of rod diameters, d_r , and jet diameters, d_j , for different water jet velocities. Here the drag coefficient, C_D , is defined as follows:

$$C_D = \frac{F}{(1/2 \rho_w v_o^2)(d_r d_j)} \quad (\text{for } d_j > d_r)$$

Table 2 summarizes the experimental conditions for each run.

Figure 11 Drag coefficient data from a series of water-jet experiments using both the apparatus of Figure 10 and an octagonal-nut force measuring apparatus

Note: The three dotted curves have been added only to help in seeing the trends of the data.

TABLE 2 - Data for Single-Rod Runs of Figure 11

Symbol on Fig. 11	V_0 (m/s)	d_j (mm)	Range of Re $\equiv \rho V_0 d_r / \mu$	Measurement System
σ	2.99	12.45	8010 - 21,360	Octag. Nut
x	2.56	9.93	6850 - 18,250	Cantil. Beam
●	2.68	9.20	7190 - 19,170	Octag. Nut
○	2.10	7.07	5610 - 14,970	Cantil. Beam
Δ	3.25	10.76	8980 - 20,770	Octag. Nut
•	3.08	10.33	8260 - 19,120	Octag. Nut
†	2.17	12.38	5800 - 15,460	Octag. Nut
φ	1.87	9.39	5010 - 13,360	Octag. Nut

There is a definite trend of decreasing C_D as the ratio, d_r/d_j , increases at constant velocity in each run. (A few dotted curves have been added to Figure 11 to help in seeing these trends.) This is probably due primarily to the increased importance of three-dimensional flow effects as the rod diameter increases toward the jet diameter. These 3-D flow effects were observed visually in these experiments and also in the previous experiments on annular liquid jets impacting small wedges [13]. In those latter experiments, as the size of a wedge increased for a fixed wedge angle, jet diameter and velocity, not only did the 3-D effects increase, but the jet splitting or spreading angle also increased. This increased 3-D spreading of a finite round jet must reduce the average pressure on the obstacle slightly as the obstacle size increases when compared to the pressure on the same obstacle in a large confined flow (or an infinite flow). However, even though this causes C_D to decrease, the total force on the obstacle and the momentum transferred both increase as the size of the obstacle increases, as expected.

There is also a general trend on Figure 11 for the runs with higher velocities to have lower C_D 's than those with lower velocities. While not all the runs follow this pattern exactly, it again suggests that the 3-D effects become more important at higher velocity; this, in turn, would tend to reduce the drag coefficient, as observed. Of course, the total force on the obstacle increases as the impact velocity is increased, although not as fast as it would if C_D stayed constant.

As a first approximation, we decided not to attempt to model the variation of C_D with d_r/d_j or impact velocity, since the results of these scoping experiments have rather large uncertainties in them [11]. Instead an overall average drag coefficient based on measurements using

IN-LINE RODS ARRANGEMENT

$$d_r = 2.381 \text{ mm}$$

$$S_L = 17.717 \text{ mm}$$

$$S_T = 8.636 \text{ mm}$$

$$N_L = 3$$

$$N_U = 9$$

STAGGERED RODS ARRANGEMENT

$$d_r = 2.381 \text{ mm}$$

$$S_L = 8.636 \text{ mm}$$

$$S_T = 17.717 \text{ mm}$$

$$N_L = 1$$

$$N_U = 8$$

Figure 12 Dimensions of the in-line and staggered rod arrays used in the water jet experiments

Figure 13 Experimental results for the in-line rod array compared to various calculated results

Note: The dotted line is the average of Fw_L and Fw_U

Figure 14 Experimental results for the staggered rod array compared to various calculated results

Note: The dotted line is the average of Fw_L and Fw_U

exchangers. Consequently, it is the most useful parameter for comparison of our results with existing data on tube banks. However, it should be noted that the actual liquid jet velocity upon impact was about $2.16 \text{ m/s} \pm 6\%$ for all the data to be presented. Consequently, the range of velocities covered in these scoping experiments was quite narrow.

The equivalent force on a tube bank with the same number of rows for an effective flow area equal to the area of the jet on impact is shown by the solid curves on Figures 13 and 14 labelled F_t . The equations from Ref. [14] were used in Ref. [11] for these tube bank calculations. It can be seen that the forces on tube banks for large confined flows (or infinite flows) is almost an order of magnitude higher than we measured for our finite round water jets. This illustrates clearly that the use of tube bank data to estimate steady-flow momentum removal from relatively small liquid slugs would be very misleading.

In an attempt to provide a better method for making an initial estimate of the momentum removed from liquid slugs by these rod arrays, we developed the following simple model. From Section 6 the average steady-state drag coefficient for a single rod impacted by a water jet was estimated to be about 0.54. The upper bound and lower bound values for the water jet force on the array of rods can then be calculated using a reference area, $d_r d_j$, for a single rod along with estimates of the number of rods involved in the momentum exchange. The estimates of the upper and lower bounds on the jet force shown in Figures 13 and 14 were then calculated as follows:

$$F_{WU} = \text{upper bound value} = N_U (0.54 \times 0.5 \rho_w V_0^2 d_r d_j) \quad (V.1.1)$$

For the steady-state period, the dimensionless impulse can be estimated from:

$$I'_a = 0.85 + 0.54 (t' - 0.5) \quad ; \quad (t' \geq 0.5)$$

It should be noted that this equation can be used whenever the total dimensionless contact time of the liquid slug with the rod exceeds 0.5 (which will be the case for all but small droplets for which our analysis is not valid) since the contact time is given by:

$$t'_c = \frac{t_c V_o}{d_r} = \frac{L_s}{d_r}$$

The fraction of the momentum removed from the liquid slug during impact with a single rod can be estimated as follows. The momentum removed is given by:

$$\Delta \text{ Mom} = I_a d_r D_s \quad (D_s > d_r)$$

Therefore, the fraction of the momentum removed is:

$$\left(\frac{\Delta \text{ Mom}}{\text{Initial Mom}} \right) = \frac{I_a d_r D_s}{m_s V_o} = \left(\frac{4 d_r}{\pi \rho_s D_s L_s V_o} \right) I_a$$

But
$$I'_a \equiv \frac{I_a}{(0.5 \rho_s V_o^2) d_r / V_o}$$

Thus
$$\left(\frac{\Delta \text{ Mom}}{\text{Initial Mom}} \right) = \left(\frac{2 d_r^2}{\pi D_s L_s} \right) I'_a$$

The above equations permit the rapid estimation of the momentum removed by a single rod. For staggered rod arrays four rows deep, the results of Section 7 can be used to estimate the total momentum removed from a cylindrical liquid slug. However, until we obtain additional

experimental data on other arrays, we must limit the use of these approximate methods to approximately the same S_T/d_r and D_S/d_r values as in the present experiments (about 7.4 and about 11 to 14, respectively for the staggered array).

For the case where $d_r < D_S \leq (2 S_T - d_r)$, the liquid slug is assumed to interact strongly with an equivalent number of rods, $N_{r(eq)} = 4.5$. This is simply the average of the upper bound (8 rods) and the lower bound (1 rod) described in Section 7. Consequently the fractional momentum removed is 4.5 times that given by the above equation for single rods.

As an example, consider a liquid slug with $L_S/D_S = 5$ and $D_S/d_r = 12$. The dimensionless contact time t_c' is equal to $(L_S/D_S) \times (D_S/d_r) = 60$, and $I_a' = 4.5 \times 33.0 = 148.4$. The fractional momentum removed is thus estimated to be about 0.13.

For large enough values of the contact time, $t_c' = L_S/d_r$, the dimensionless impulse can be approximated by $I_a' \approx 0.54 N_{r(eq)} t'$ ($t' \gg 0.5$). Putting this in the momentum removal equation, we obtain the simplified, approximate result:

$$\frac{\Delta \text{ Mom}}{\text{Initial Mom}} = \left(\frac{2 \times .54}{\pi} \right) \left(\frac{d_r}{D_S} \right) N_{r(eq)} = 0.344 \left(\frac{d_r}{D_S} \right) N_{r(eq)}$$

For the above example this gives a fractional momentum removal of 0.129 for $t_c' = 60$, which is in excellent agreement with the more exact result above.

These results must be considered tentative until more extensive experiments are run. However, they are felt to be very much better than estimates based on tube bank pressure drop data.

9. CONCLUSIONS AND RECOMMENDATIONS

The problem of momentum removal from slugs of liquid lithium using arrays of cylindrical rods has been investigated and small-scale scoping experiments have been run. Our experimental results permit a first estimate to be made of the magnitude of the momentum removal possible with typical staggered arrays of rods four rows deep (or in-line arrays three rows deep). A numerical example shows that about 13% of the liquid slug momentum may be able to be removed by a rather loosely packed array of staggered rods.

However, many unanswered questions remain including:

- a) The actual size and shape of the liquid lithium slugs and the effect on momentum removal.
- b) The effect of much higher liquid slug velocities on the momentum transfer.
- c) The effect of any vapor bubbles in the slugs.
- d) The conditions for cavitation upon impact and the effect on momentum transfer.
- e) The optimum array arrangement and optimum number of rod rows to use.
- f) Stresses and lifetime of the rods for different attachment methods.

These are but a few of the interesting questions raised by this concept for extending the lifetime of the HYLIFE vacuum vessel proposed by the LLNL engineers. If the concept is to be incorporate in the HYLIFE design, additional small-scale experimental studies will be necessary before such a design can be done with a high degree of confidence.

ACKNOWLEDGEMENTS

This research was supported by the Systems Studies Group of the Laser Program at the Lawrence Livermore National Laboratory (LLNL) under Intramural Purchase Order No. 4794109. We wish to express our special thanks to Michael Monsler, Jack Hovingh, Wayne Meier, John Pitts, Jim Blink and others for their support and many helpful suggestions.

REFERENCES

- 1a. L. W. Coleman and W. F. Krupke, eds., "1980 Laser Program Annual Report", Lawrence Livermore National Laboratory, June 1981, Vol. 3, Sec. 9.
- 1b. L. A. Glenn and D. A. Young, "Dynamic Loading of the Structural Wall in a Lithium-Fall Fusion Reactor", Nuclear Engineering and Design, 1979, Vol. 54, (Hugoniot Press) pp. 1 to 16.
2. K. Souter and H. Krachman, "Water Impact Tests of Rigid and Flexible Cylinders", EPRI Report NP-798, Project 817, Palo Alto, CA, May 1978.
3. M. B. Gross, T. L. Gears, P. V. Marcal, C. W. Hirt, and B. D. Nichols, "Hydrodynamic Impact Analysis", EPRI Report NP-824, Projects 812-1, 2, 3, and 965-3, Palo Alto, CA, June 1978.
4. C. Dalton and J. M. Nash, "Wave Slam on Horizontal Members of an Offshore Platform", Paper No. OTC 2500, Proceedings of the Offshore Technology Conference, 1976, pp. 769-780.
5. P. Kaplan and M. N. Silbert, "Impact Forces on Platform Horizontal Members in the Splash Zone", Paper Number OTC 2498, Proceedings of the Offshore Technology Conference, 1976, pp. 749-758.
6. O. Faltinsen, O. Kjoerland, A. Nottveit, and T. Vinje, "Water Impact Loads and Dynamic Response of Horizontal Circular Cylinders in Offshore Structures", Paper Number OTC 2741, Proceedings of Offshore Technology Conference, 1977, pp. 119-126.
7. T. Sarpkaya, "Wave Impact Loads on Cylinders", Paper Number OTC 3065, Proceedings of Offshore Technology Conference, 1978, pp. 169-176.
8. H. Schlichting, "Boundary Layer Theory", Fourth Ed., McGraw-Hill, NY, 1960.
9. S. Brodetsky, "Discontinuous Fluid Motion Around Circular and Elliptical Cylinders", Proceedings of the Royal Society of London, Series A, Volume 102, 1923, pp. 542-553.
10. R. Bullis et al., "Inertial Confinement Fusion Research and Development Studies", Final Rpt. Grumman Aerospace Corp., August, 1980.
11. A. R. Raffray, "Water Jet Impact Experiments", M. S. Thesis, Dept. of Mech. Engr., Univ. of Calif., Davis Campus (in preparation).
12. M. A. Hoffman, "Liquid Jet Experiments: Relevance to Inertial Confinement Fusion Reactors", Proc. of the 9th Symp. on Eng. Probs. of Fusion Research, IEEE Pub. No. 81CH1715-2-NPS, Vol. II, pp. 1901-1904, October 1981.

13. M. A. Hoffman, R. K. Takahashi, and R. D. Monson, "Annular Liquid Jet Experiments", ASME J. of Fluids Eng., Vol. 102, Sept. 1980, pp. 344-349.
14. F. Kreith, Principles of Heat Transfer, Third Edition, International Textbook Co., New York, 1973, pp. 478-482.