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20. ABSTRACT (*Continue on reverse side if necessary and identify by block number*)

The USAF School of Aerospace Medicine (USAFSAM) Crew Technology Division has developed an advanced anti-G valve for pressurizing the anti-G suit during exposures to acceleration. The anti-G valve presently in fighter aircraft has been determined to operate too slowly for rapid onset of G, potentially causing pilots of high-performance aircraft to black out, lose consciousness, and/or become fatigued. The time relationship to G-suit pressurization using the conventional anti-G valve was found to be sigmoidal, having two relatively

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20. ABSTRACT (continued)

slow pressurization phases--one early, and the other late--in the suit-inflation schedule. Elimination of these two slow phases was accomplished by: (a) preinflating the anti-G suit to 0.2 psi prior to an increase in G (called "Ready Pressure"); and (b) increasing the capacity of air flow through the anti-G valve (called "Hi-Flow"). The development of the Hi-Flow Ready Pressure (HFRP) anti-G valve by USAFSAM increased the rate of G-suit pressurization threefold. This HFRP anti-G valve was tested on eight F-15 pilots, using the centrifuge at the Naval Air Development Center, Warminster, PA. A comparison of this experimental valve with the conventional anti-G valve (presently operational in the F-15 aircraft) resulted in a high degree of pilot acceptance, because the HFRP valve had better valve response, reduced valve error scores, and allowed the pilots to tolerate high-G exposures with less effort.

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CONTENTS

Figures

Tables

A NEW ANTI-G VALVE FOR HIGH-PERFORMANCE AIRCRAFT

INTRODUCTION AND BACKGROUND

The F-15 anti-G valve is similar basically to the anti-G valves used in jet fighter aircraft two decades ago. It was not surprising, therefore, that--due to the increase in performance characteristics of the F-15--the inflation rate of the anti-G suit was found to be too slow for pilots flying this advanced fighter aircraft. Consequently, the USAF School of Aerospace Medicine (USAFSAM) initiated development of an anti-G valve which would significantly surpass the performance of the valve presently in the F-15. The approach to and results of this research and development program are reported herein.

APPROACH

The rate of anti-G suit inflation is slowed by two passages of high resistance to air flow--the valve itself, and the suit hose connecting the valve to the suit abdominal bladder. Reducing or eliminating these resistances would increase suit inflation rates; yet, too rapid inflation of the G-suit could be uncomfortable to the pilot.

The resistance of the G-valve was reduced by increasing the sizes of several ports within the valve (Fig. 1). This modification increased air flow through the valve by approximately 50%; for the air flow through the standard valve is approximately 15 ft^3/min ($0.42\ m^3/min$), whereas the modified hi-flow valve has 22 ft^3/min ($0.62\ m^3/min$) air flow. This modification was made by Alar Products, Inc.[1] under a USAFSAM contract. The modified valve is called the "Hi-flow" (HF) valve.

The resistance of the hose of the anti-G suit was circumvented by allowing the suit to fill with air to a pressure of 0.2 psi prior to an increase in G. This pre-G inflation allows for approximately 60% of the suit volume to be filled with air, thereby greatly reducing the amount of air which must pass through the suit hose to increase suit pressure. This pre-G inflation technique is called "Ready Pressure" (RP).

A ready pressure device has been developed at USAFSAM (Fig. 2). This device is adjustable, so that ready pressure can be provided to the suit at 1 G from 0.1 to 1 psi (0.2 psi appears to be favored by most pilots). When 2 G is obtained, the ready pressure device becomes inoperable;

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[1] Alar Products, Inc., 9100 Valleyview Rd., Macedonia, OH 44056

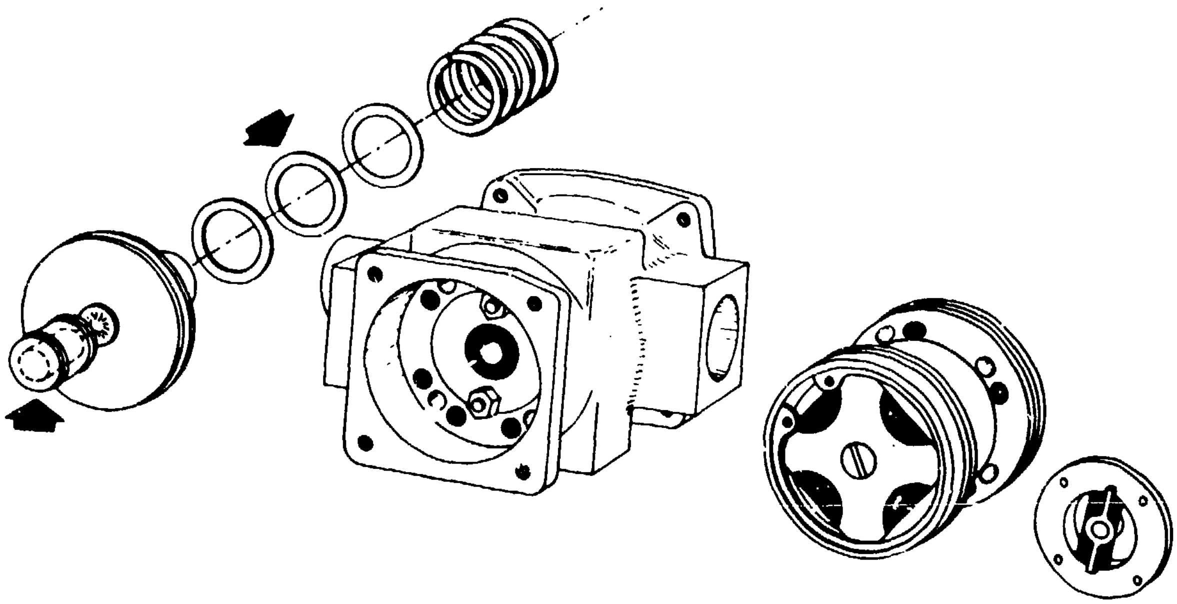
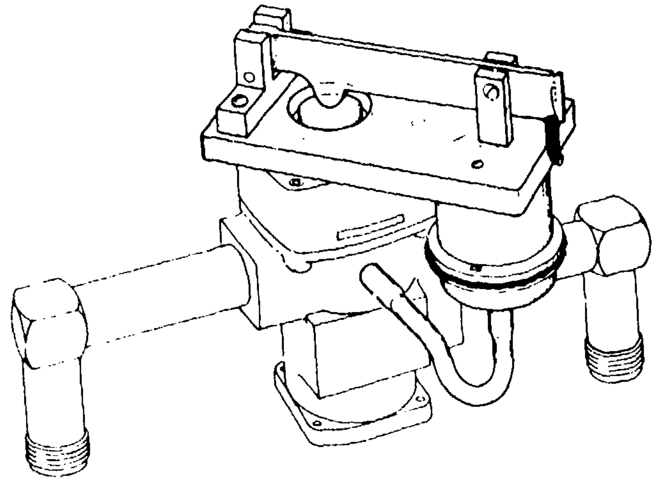
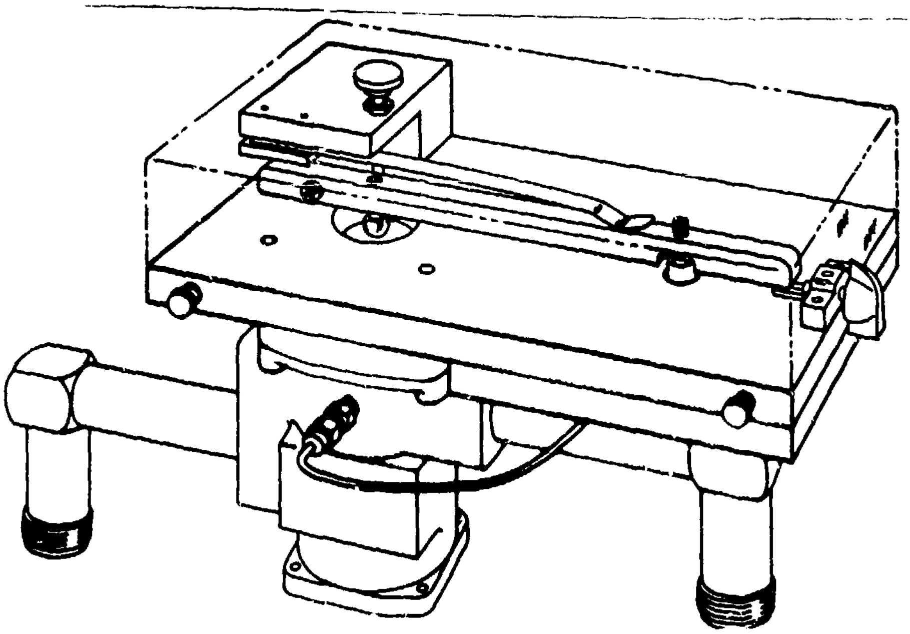


Figure 1. Schema of the Alar anti-G valve modification (shown in black). Arrows indicate additional modifications--an enlarged spiracle, and 3 spacers.



View 2-A. "Brassboard" model of HFRP anti-G valve used in our study.



View 2-B. "Prototype" model of the HFRP anti-G valve tested on the USAFSAM centrifuge.

Figure 2: Views A and B. These devices maintain suit pressure at 1 G with spring tension against the valve's "press to test" button. Ready pressure is controlled (± 5mm Hg) via a diaphragmed chamber connected by a tube to the anti-G valve which monitors the G-suit pressure. The prototype model (View 2-B) has an on/off switch for ready pressure.

and it remains so until the acceleration force is once again equal to or less than 2 G. If ready pressure is not needed or desired by the pilot, it can be eliminated with an on/off switch. The ready pressure device can be mated to the hi-flow anti-G valve or the standard F-15 valve, with minor valve modifications.

EXPERIMENTAL PLAN

Little is known about methods to test anti-G valves appropriately so that their function can be quantified in some meaningful manner during aerial combat maneuvers (ACM). Typically, anti-G valves and suits are tested to determine their effect on G tolerances of relaxed subjects. Such tests measure the cardiovascular effect of the anti-G system. Unfortunately, they do not determine the support offered the pilot at rapid G-onset rates or to high G (>6.0 G) where he may spend a significant portion of his time during the ACM.

Much of the data relative to G-valve quantification must be obtained from G-suit response, for the suit is that component of the anti-G system which directly provides G-protection. Consequently, a physiologic test plan was developed using G-suit responses to determine if hi-flow, ready pressure, or combined hi-flow ready pressure would be a significant improvement over the valve presently in the F-15. Two major criteria of the test to demonstrate an improved valve were to quantify: (a) G-protection, and (b) pilot acceptance. Pilot acceptance in this regard is critical, especially because G-suits and valves are dynamic equipment that covers a portion of the body and shares the cockpit with the pilot. Of course, since pilot acceptance would include some evaluation of G-protection, final valve selection used a combination of both criteria.

Subject Selection

Because there is no known objective measure of pilot acceptance and G-protection of suits and valves that critically evaluate these parameters during ACM, much of the data had to be subjective. Subjective data, however, may be extremely biased; therefore, determining pilot prejudices regarding the F-15 anti-G system was important. Selection of the type of test subjects to be used in this study was also very important. Since pilot acceptance of the valve to be installed in the F-15 was critical, F-15 pilots were the subjects of choice. Fortunately, 8 volunteer F-15 pilots, from 2 USAF bases, were provided by the Tactical Air Command (TAC) for this study.2/

2/ We gratefully acknowledge the fine cooperation of the participating F-15 pilots from TAC: Capt. C. V. Bradford, Capt. T. R. Butler, Capt. R. E. Doehling, Capt. G. R. Gore, Capt. D. C. Hayes, Capt. S. J. Knight, Capt. N. L. Schoening, and 1st Lt. M. R. Judge.

Immediately prior to this study, each pilot's flying history and physical condition were determined through a questionnaire which identified: (a) age and body size; (b) flying experience in the F-15; (c) exercise routine; and (d) present physical condition. Each pilot's biases regarding the anti-G system presently in the F-15 were likewise determined by obtaining his opinions on: (a) F-15 G-valve adequacy; (b) G-suit adequacy; (c) qualitative value [illegible] an anti-G system; and (d) G-valve reliability. During the study, [illegible] questionnaires determined daily the pilot's fatigue status and previous night's sleep. Biases of the pilots, concerning the specific valves to be tested, were minimized by keeping the subjects and the principal investigator completely "blind" during the entire week of data collection.

Study Location

The physical test-bed for the study was also very important. Two possibilities existed: (a) to test the valves in the F-15 aircraft during aerial combat maneuvers; or (b) to use a centrifuge where G regulation would be more precise. The flight-test approach was attractive in providing the "real world" environment, but several difficulties made it unacceptable for the initial developmental test of these valves. Specifically, difficulty in obtaining approval for the experimental valves for F-15 flight was a major factor; and, since much of the data of this study would be developed from daily comparisons of different valves, closely regulated and reproduced G-profiles were essential. This condition was only possible using the centrifuge.

The specific centrifuge to be used also had to be determined. Earlier studies at USAFSAM, using similar anti-G valves, had found that the F-15 anti-G valve performed well at 1 G/sec onset rates. This valve became deficient only at 3 G/sec and higher G-onset rates. The only centrifuge in the United States capable of exposing humans to these rapid G-onset rates was at the Naval Air Development Center (NADC), Warminster, PA. Consequently, the study was performed with the cooperation of the US Navy, using its centrifuge at NADC.3/

Valve Selection

The ready pressure device, in combination with the hi-flow valve and standard F-15 valve, made it possible to have 3 experimental valves with operational characteristics superior to the F-15 valve. These experimental valves were: (a) hi-flow valve alone; (b) hi-flow valve with ready pressure; and (c) F-15 valve with ready pressure. However, in order to maintain the double-blind aspect of this study (since ready pressure application would be obvious to the pilot during the test),

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3/ We gratefully acknowledge the use of the centrifuge facilities at NADC. The study reported herein would not have been accomplished without the use of the NADC centrifuge and the fine cooperation of those responsible US Navy personnel.

an additional valve with ready pressure--having overall poor performance characteristics--was needed in these tests as a ready pressure control valve. This ready pressure control valve, as a fourth experimental valve, would provide the answer to the question: Can the pilot discriminate overall improved valve function from the obvious ready pressure?

Subjective data must be compared to a known standard if these data are to be quantifiable. Since we were concerned about the function of our 3 experimental valves relative to the USAF F-15 valve, we had each pilot compare the responses of our experimental valves to the valve in his F-15. We also included an F-15 valve (unknown to the pilots) as a fifth valve among the valves to be tested during the week of our study.

A pilot tested only one valve each day, and the valves were randomized on a daily basis among the pilots. Five days of testing were required to complete the data collection, since 5 valves were to be tested--(a) F-15 valve alone; (b) F-15 valve with ready pressure; (c) hi-flow valve alone; (d) hi-flow valve with ready pressure; and (e) ready pressure control valve. Each pilot provided his own anti-G suit for the study.

Acceleration Profiles

Since the F-15 valve was reported to be deficient only at rapid G-onset rates, the G exposures used to test the valves were critical. Five acceleration exposures were used, and always in this order: (a) 3 G for 15 sec--1 G/sec onset rate; (b) 7 G for 10 sec--1 G/sec onset rate; (c) 7 G for 10 sec--3 G/sec onset rate; (d) 7 G for 10 sec--6 G/sec onset rate; and (e) a simulated ACM with three 7 G peaks with all increases in G at a 6 G/sec onset rate (Fig. 3). Rests between

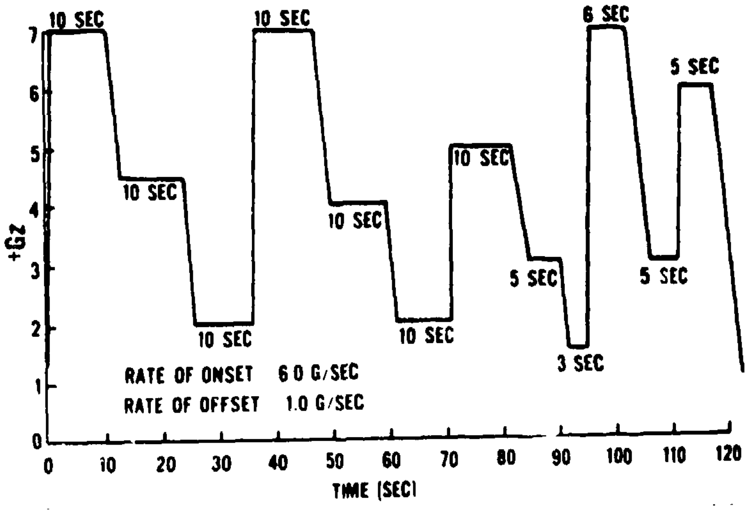

Figure 3. The ACM has three 7-G peaks, with all increases in G at 6 G/sec. Midway through the ACM, the G level was reduced below 2 G so that the effect of ready pressure would be felt twice by the pilots during each ACM exposure--the G-valve without ready pressure does not begin to inflate the G-suit until 2.2 G.

G exposures were determined on the basis of individual pilot preference, and usually were less than 30 sec. A 6 G/sec onset rate to 7 G was chosen because it was the highest onset rate possible at NADC without large G overshoots. At the completion of the study, the F-15 pilots noted that 6 G/sec would be the maximum they would use in an ACM. Several pilots thought 3-4 G/sec onset rates would have been more realistic.

The use of an ACM as a G profile on the centrifuge has been found by USAFSAM to be more appropriate for testing, relevant to high performance aircraft, than the sustained G exposures more common to acceleration research. These high-G profiles including the ACM were considered by the pilots to be quite realistic, and a valid approach for testing the G-valve. In the first questionnaire, the pilots noted that they primarily used the G-suit to support their M-1 maneuver at high G.

Subjective Criteria in Valve Evaluation

Fatigue--Immediately before and after each pilot's daily centrifuge valve test, his fatigue status was quantified by scoring 10 questions regarding his feeling of physical well-being. The difference in the before and after fatigue scores was the net fatigue produced by the day's 5 acceleration exposures.

Effort--Pilot effort was quantified by having the pilot mark a chart scaled from moderate effort to maximum effort. Moderate effort had a grade of 1, whereas maximum effort was recorded as 7. Any level of effort between these extremes could be marked accordingly, so that a graded effort scale was developed.

Suit Pressure at 7 G--The feel of G-suit pressure by the pilot while he was at 7 G was graded, using the method employed for deriving an effort score. The extremes for suit pressure ranged from too little (grade 1) to too much (grade 7). Perfect suit pressure would have had a grade of 4.

Suit Support During G Onset--Since the pilot was exposed to the respective onset rates of 1, 3, and 6 G/sec to 7 G (exposures 2, 3, and 4), the support that the suit offered during each of these onset rates was subjectively scored in a manner similar to that already described (in the paragraph on "Effort"). The minimum score of 1 was "poor"; and "excellent" received a grade of 7. The pilot was also asked if his valve offered ready pressure.

Experimental Valve Comparison With F-15 Valve--The pilot was asked to rate the valve tested against his valve in the F-15. This rating was then scored in a manner similar to the effort and suit pressure at 7 G evaluations, with the extremes being "much worse" and "much better." Much worse received a score of 1; and much better, 5. A score of 3 meant that the valve tested was similar to (same as) the F-15 valve. This valve rating score is termed "F-15 valve comparison" in this report.

Valve Comparison With All Valves Tested--After the last valve was tested by each pilot on Friday, the pilot was asked to list the valves (days) in order of preference. He was still "blind" regarding the valves he had tested, so that he could only identify valve function with the day of the week on which a specific valve had been tested. The best valve (day) received a score of 1; and the poorest valve, 5. This type of valve rating score is termed "rank-preference" throughout this report.

Pilot Acceptance--After the study was concluded, and while the pilot was still blind regarding the valves tested, he answered several questions regarding acceptance in the F-15 of the valves he had tested during the week. These questions also elicited his opinions on Ready Pressure.

An additional opinion of pilot acceptance of our USAFSAM anti-G valves was solicited after the pilot had returned to his base and was once again flying the F-15. Of course, this additional opinion was not blind; for the pilot had now been told about the study. The primary question was: "Now that you have flown the F-15 again after our study (and I'm sure you felt the G-suit inflate), do you (still) think that at least one of our experimental G-valves is better than the one now in the F-15? If so, please identify that valve."

Objective Criteria in Valve Evaluation

Physiologic Responses--Since the G-suit significantly affects physiologic responses to high G exposure, several physiologic parameters were determined and compared among the valves tested. These parameters included: (a) heart rhythm; (b) various measures of heart rates; and (c) maximum level of light loss, as reported by the pilot after each G exposure.

G-suit Inflation (Valve Error Scores)--An objective measure of valve response was devised and called the "valve error score." The analog voltage response for the accelerometer (G-profile) was compared to the analog voltage response of the G-suit pressure inflation profile. If the G-suit inflation rate was immediate, it followed the G profile exactly. If, however, suit inflation was slowed by a poorly performing G-valve, the G-suit inflation profile lagged behind the G-profile. This area of lag, called the valve error score, is indicated in Figure 4 as the G-suit inflation error. Integration of this area resulted in a number without physical dimensions. The greater the valve error score, the poorer the G-valve performance. Valve error scores were calculated for all valves at the three different G-onset rates to 7 G.

G-suit Inflation (Maximum Inflation Rates)--Quite apart from the "valve error scores," 4 valves (F-15, hi-flow, F-15 with ready pressure, and hi-flow with ready pressure) were "bench tested" in the USAFSAM Crew Technology Division laboratory using maximum air flow capability. Each valve was connected to an anti-G suit with its volume restricted to approximately 10 liters. The pressure inside the suit's abdominal bladder was monitored, using a PM 131TC Statham Pressure Transducer, via a small rubber tube introduced into the abdominal bladder through the G-suit hose. The analog output of the transducers was recorded on a 4 Channel Brush Recorder. The pressure source to the valve was a 14-liter air reservoir at 70 psi, connected to the G-suit by reinforced plastic tubing (6 ft long X 1/2 in. id; or 182.88 cm X 1.27 cm). The reservoir was fed by laboratory line air at 70 psi through reinforced rubber tubing (2 ft long X 1/4 in. id; or 60.96 cm X 0.64 cm). The valve was manually activated instantaneously to its maximum flow capability.

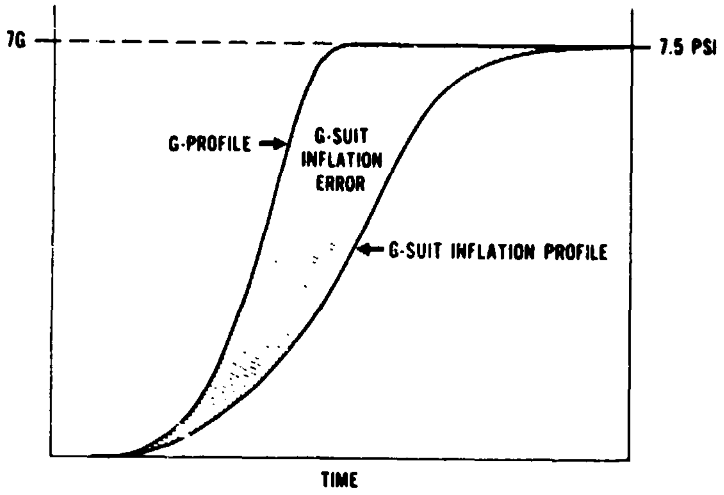

Figure 4. The method of determining "G-suit inflation error" (valve error score). Integration of this difference between the G-profile and G-suit inflation profile results in the valve error score which is without physical dimensions.

Correlation of Valve Evaluation Criteria with Valve Selection

Since little is known regarding appropriate methods to test the effectiveness of anti-G valves, the evaluation criteria used in these studies to test valve function were statistically correlated with the two types of valve selection methods: (a) F-15 valve comparison, and (b) rank-preference. A statistical correlation was also considered between those evaluation criteria which were found statistically to discriminate significantly the various valves tested. This information would be of value in selecting useful criteria for other future valve testing programs.

RESULTS

Summarized in Table 1 are the responses of the eight F-15 pilots to our initial questionnaire at the beginning of the study. Clearly, the pilots were not satisfied with the anti-G valve in the F-15, although the valve was considered to be reliable. The major problem regarding the G-suit was excessive bulging of the suit abdominal bladder, and the majority of the pilots thought this problem rendered the anti-G suit inadequate. They considered the anti-G suit most useful as a support unit for their M-1, and less useful as a method to increase relaxed G-tolerance.

TABLE 1. INFORMATION PROFILE OF EIGHT F-15 PILOTS USED TO TEST THE 5 ANTI-G VALVES

PILOT AGE AND SIZE:

	Mean	Range
AGE (yrs)	28.8	25-32
HEIGHT (in.)	70.6	66-75
WEIGHT (lbs)	167	140-190

PHYSICAL CONDITION: Excellent--2; Good--6

FLYING EXPERIENCE:

Aircraft	Mean (hrs)	Range (hrs)
F-15	151	20 - 350
F-4	487	0 - 1150
Others	567	50 - 1280

PILOTS' OPINION OF F-15 ANTI-G VALVE ADEQUACY:

Yes--0; No--7; No Opinion--1

The principal complaints were that: valve response was too slow, and started too late, with rapid onset of G.

PILOTS' OPINION OF ANTI-G SUIT ADEQUACY:

Yes--3; No--5

Excessive bulging of the C-suit abdominal bladder was the major complaint.

PILOTS' USE OF THE ANTI-G SUIT:

6 pilots felt that the G-suit was useful to increase relaxed tolerance and support the M-1; and 2 pilots used the suit to support the M-1 only.

PILOTS' EXPERIENCE REGARDING ANTI-G VALVE RELIABILITY:

2 pilots had 1 anti-G valve failure, respectively; and the other 6 pilots never had an anti-G valve failure.

Physiologic Responses

Light Loss--The maximum loss of peripheral and central lights was recorded for each pilot after each 7-G run. The maximum loss of lights neared 100% (black-out), on occasion. No significant differences occurred in light loss relative to rate of G onset, type of 7-G profile, or type of valve used. This finding indicated that the pilots maintained the same arterial pressure at eye-level during 7-G exposures, regardless of the anti-G valve used.

Heart Rate--Heart rates for each pilot were measured using 2 leads of EKG (sternal and biaxillary) and cardiotachometer. The following maximum and mean heart rates were determined and compared with the 5 anti-G valves tested: (a) before and during each G profile, and (b) after the ACM exposure for 5 min. No significant differences in heart rates relative to the valve tested were found. The mean resting pre-G heart rate for 8 pilots was approximately 90 - 100 bpm. Maximum mean heart rate for each 10-sec 7-G exposure was approximately 130 bpm, whereas during the ACM the mean heart rate at 7 G reached 165 - 180 bpm. Heart-rate recovery occurred rapidly after the ACM, returning to pre-G levels within 3 min.

Heart Rhythm--Irregular heart beats were identified using the analog recording of the 2 leads of EKG which had been obtained from each pilot each day. These irregular beats were grouped according to their cardiac origin (supraventricular or ventricular) and relative to each G-valve. No significant differences were found regarding the G-valves, although irregular heart beats (atrial and ventricular premature beats) during G exposure were quite common (Table 2). No serious heart arrhythmias were identified.

Fatigue--These 5 successive G-exposures in rapid order did not produce significant fatigue in any of the pilots using any of the valves (Table 2). However, the greatest amount of fatigue for a valve group did occur with the standard F-15 anti-G valve. Exposing the pilots to a fatiguing amount of G was not the intent of this study. Their mental concentration was required for evaluating G-suit inflation--not for fighting fatigue.

Effort--The effort required to maintain vision, as measured subjectively during 7-G exposures, was significantly greater ($P < 0.05$) for pilots using the F-15 anti-G valve than for those using the hi-flow ready pressure valve (Table 2).

Effort scores were significantly (directly) correlated with several other subjective determinations: (a) G-suit inflation support at 3 G and 6 G/sec onset rates to 7 G; (b) G-suit pressure while at 7 G; and (c) G-valve selection using either "rank-preference" (inversely correlated) or "F-15 valve comparison" (Table 3). The pilots considered effort rather heavily in selecting their valve preferences. The coefficients of determination (correlation coefficient squared) for rank preference was 0.40 and 0.46 for F-15 valve comparison; i.e., valve selection was based

TABLE 2. PHYSIOLOGIC RESPONSES OF F-15 PILOTS TO THE 5 ANTI-G VALVES TESTED (MEAN ± SE)

Valve	Fatigue[a]	Effort[a]	Heart rhythm[b] SV	Heart rhythm[b] V
F-15[c]	2.6 ± 1.23	5.1 ± 0.63	1.3 ± 0.5	0.8 ± 0.3
F-15 + RP[d]	0.6 ± 0.71	4.7 ± 0.60	0.8 ± 0.4	1.3 ± 0.8
RPC[e]	0.8 ± 0.72	4.0 ± 0.53	0.9 ± 0.4	0.5 ± 0.2
HF[f]	1.1 ± 0.74	3.7 ± 0.59	2.1 ± 1.4	0.9 ± 0.5
HF + RP	1.2 ± 0.88	3.1 ± 0.61	1.7 ± 0.7	0.1 ± 0.1
F-15 vs. HF+RP ($P<$)	N.S.	0.05	N.S.	N.S.

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[a] Subjective scores (refer to section on "Methods");
[b] number of irregular heart beats per pilot per day (SV = supraventricular origin; and V = ventricular origin of irregular beats);
[c] F-15 = valve in F-15;
[d] RP = ready pressure;
[e] RPC = ready pressure control valve; and
[f] HF = hi-flow valve.

TABLE 3. CORRELATION COEFFICIENTS OF PARAMETERS USED TO EVALUATE ANTI-G VALVES VS. VALVE SELECTION METHODS (F-15 COMPARISON OR RANK PREFERENCE)

Evaluation parameter	F-15 comparison	Rank preference
Error Scores:		
1 G/s	-0.28[a]	0.18
3 G/s	-0.37[b]	0.27
6 G/s	-0.39[b]	0.34[b]
Subjective Valve Response:		
1 G/s	0.59[c]	-0.45[c]
3 G/s	0.79[c]	-0.58[c]
6 G/s	0.77[c]	-0.61[c]
Effort	-0.68[c]	0.63[c]
Suit Pressure at 7 G	0.26	-0.39[b]

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[a] Correlation coefficients with 37 degrees of freedom;
[b] $P < 0.05 = 0.317$, or greater; and
[c] $P < 0.001 = 0.408$, or greater.

40% to 46% on the amount of effort used during the G exposures. Effort was also directly correlated ($P < 0.05$) with the calculated valve error scores, but only at 6 G/sec onset rates to 7 G.

G-Valve Function

Suit pressure at 7 G--The support at 7 G offered by the G-suit pressure, as subjectively quantified by the pilots, is shown in Table 4. Both hi-flow valves offered significantly ($P < 0.01$) better support at 7 G than did the F-15 valve, even though G-suit pressure at 7 G was the same for all of the valves. Apparently, the delay in the pressurization of the suit at 7 G, due to the slow functioning F-15 valve, was translated into a difference in total suit pressure at 7 G. This finding was borne out with a significant direct correlation ($P < 0.01$) between G-suit pressure at 7 G and valve response at 6 G/sec. However, G-suit pressure at 7 G was only slightly considered by the pilots for valve selection--$P < 0.05$ regarding rank preference (coefficient of determination of only 0.15), and no significant correlation for F-15 valve comparison (Table 3).

TABLE 4. G-SUIT SUPPORT SCORES OFFERED BY THE 5 ANTI-G VALVES AS SUBJECTIVELY DETERMINED BY PILOTS (MEAN ± SE)

Valve[a]	Suit pressure	Valve response		
	at 7 G	1 G/sec[b]	3 G/sec[b]	6 G/sec[b]
F-15	2.6 ± 0.41	4.2 ± 0.40	3.9 ± 0.46	3.7 ± 0.54
F-15 + RP	3.4 ± 0.48	4.2 ± 0.70	4.1 ± 0.67	4.0 ± 0.65
RPC	3.1 ± 0.22	4.8 ± 0.44	4.7 ± 0.41	4.7 ± 0.45
HF	4.1 ± 0.35	4.0 ± 0.68	4.5 ± 0.62	5.0 ± 0.53
HF ± RP	3.7 ± 0.31	5.7 ± 0.24	5.5 ± 0.26	5.6 ± 0.41
F-15 vs. HF + RP (P<)	0.01	0.01	0.02	0.02

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[a] Valve identification is shown in Table 2.
[b] 1, 3, or 6 G/sec onset rates to 7 G.

Valve Responses At Different G-Onset Rates--Quickness of valve response, as subjectively determined by the pilots, is shown in Table 4. At 1 G/sec onset rate, the F-15 valve performed in a satisfactory manner. This finding is not surprising, since the valve was found deficient in the aircraft only at rapid G-onset rates. As the rate of G onset increased, however, the F-15 valve response deteriorated. By comparison, the hi-flow ready pressure valve maintained a significantly ($P < 0.01$ - 0.02) improved valve response at all G-onset rates--remaining superior to all of the other valves tested.

Correlation analysis found various valve responses to be highly correlated ($P < 0.01$) between the different G-onset rates, as might be expected (Table 3). Also, as might be expected, valve response was heavily considered by the pilots in their selection of valves. The coefficient of determination concerning valve response at 6 G/sec was 0.37 for rank-preference, and 0.59 for F-15 valve comparison. Approximately 60% of valve selection, comparing the valves tested with the valve in the F-15, was based on valve response during rapid G-onset.

Valve Error Scores--Valve errors (G-suit inflation error) at various G-onset rates, as quantified using evaluations described previously (Fig. 4), are in Table 5. The F-15 valve had the largest valve error scores of the valves tested. The hi-flow ready pressure valve had significantly ($P < 0.001$) smaller error scores than the F-15 valve. However, the hi-flow ready pressure valve did not have the lowest error scores; the electronically controlled ready pressure control valve consistently had the best error scores. These small error scores were recorded even though this valve had been delayed by 0.2 sec at G onset to better resemble the F-15 valve Obviously, a greater delay should have been programmed into that valve, so that it would have better approximated the slowness (valve error scores) of the F-15 valve.

TABLE 5. VALVE ERROR SCORES AS CALCULATED [REFER TO TEXT AND FIG. 4 (MEAN ± SE)]

Valve[a]	1 G/sec[b]	3 G/sec[b]	6 G/sec[b]
F-15	2.7 ± 0.09	2.3 ± 0.18	2.4 ± 0.21
F-15 + RP	2.1 ± 0.10	1.7 ± 0.16	1.[illegible] ± 0.12
RPC	0.9 ± 0.09	1.1 ± 0.13	1.5 ± 0.16
HF	2.4 ± 0.08	2.0 ± 0.10	1.9 ± 0.10
HF + RP	2.1 ± 0.05	1.6 ± 0.07	1.6 ± 0.08
F-15 vs. HF + RP ($P<$)	0.001	0.001	0.001

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[a] Valve identification is shown in Table 2.
[b] 1, 3, or 6 G/sec onset rate to 7 G.

Interestingly, this electronic valve--which had a small error score--did not rank high during valve selection by the pilots. The slight programmed delay in valve response must have been considered by the pilots as a reduction in total valve function. Along this line of pilot selection of anti-G valves, valve error scores were only barely significantly correlated ($P < 0.05$), with valve selection offering coefficients of determination of 0.12 and 0.15 regarding rank-preference and F-15 valve comparison (Table 3). Although valve error scores are objective measures of valve response during G-onset rates, these scores do not appear to measure accurately the valve response as determined subjectively by the pilots.

Maximum G-Suit Inflations--The maximum rates of pressurization (inflation) of the G-suit for four types of anti-G valves, using the "bench test" approach, are shown in Figure 5. The G-suit inflation schedule of the valve presently in the F-15 is sigmoidal, thus indicating the existence of two slow phases: one, early in the inflation schedule (suit inflation up to 1 psi); and the other phase, later in the schedule (suit pressure above 7 psi). A rapid suit-inflation phase occurs between the two slower phases. In order to improve G-suit inflation rates significantly, both slow phases have to be modified (improved) because they consumed approximately 75% of the total G-suit inflation time. Of course, an increase in the rapid suit-inflating phase (G-suit pressures from 1 to 7) would also be beneficial, and would improve G-suit inflation rates.

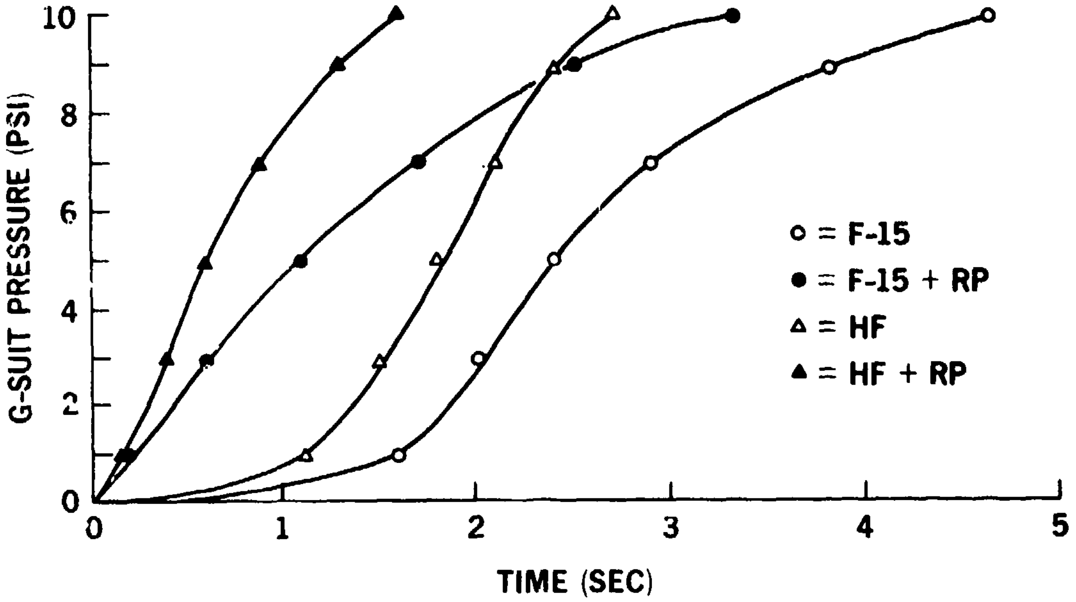

Figure 5. Maximum G-suit inflation schedules for four anti-G valves. (For key to abbreviations, refer to Table 2 footnote.)

The early lag phase was reduced using the Ready Pressure (RP) concept of inflating the suit prior to G exposure. The benefit of RP only, without the hi-flow valve, was determined by fitting the RP unit onto an F-15 anti-G valve. The effect of RP is shown in Figure 5 (F-15 + RP curve). Clearly the early lag phase is eliminated; however, the remaining two phases of suit inflation are not significantly affected, for the suit inflation schedule parallels the G-suit inflation profile of the F-15 anti-G valve without RP.

The rapid suit-inflation phase, and the following slow inflation phase, were improved considerably with the use of the hi-flow valve alone without RP (Fig. 5). As expected, without RP, the early slow phase of the F-15 anti-G valve remains even with the use of the hi-flow valve. Consequently, the combination of the hi-flow and RP results in a G-suit inflation profile that is nearly optimum (Fig. 5).

Pilot selection of the various G valves is nicely correlated with these maximum G-suit inflation rates relative to the length of time required to inflate the G-suit to 7.5 psi (suit pressure at 7 G). Both HF and F-15 + RP valves required approximately the same amount of time (2 sec) to inflate the G-suit to 7.5 psi (Fig. 5); and both valves received similar pilot selection scores (3.1 and 3.2), based on either F-15 comparison or rank performance valve selection criteria (Table 6). Likewise, HF + RP and F-15 valve function (Fig. 5) and valve selection (Table 6) were directly correlated. HF + RP received the highest marks, and the F-15 valve scored poorly.

TABLE 6. VALVE SELECTION SCORES BY THE PILOTS, AS BASED ON TWO SUBJECTIVE METHODS OF VALVE QUANTIFICATION

Valve[a]	F-15 comparison	Rank preference	Pilots' choice[b]
F-15	2.5 ± 0.33	4.2 ± 0.33	0
F-15 + RP	3.1 ± 0.40	3.1 ± 0.52	1
RPC	3.8 ± 0.27	2.8 ± 0.41	1
HF	3.2 ± 0.36	3.2 ± 0.53	1
HF + RP	4.2 ± 0.16	1.6 ± 0.32	5
F-15 vs. HF + RP ($P<$)	0.001	0.001	---

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[a] Valve identification is shown in Table 2.
[b] Pilots' choice of the anti-G valve they would prefer to have on the F-15.

G-Valve Selection

The final selections of the best anti-G valves, as determined by the F-15 pilots and as based on two separate subjective approaches, were quite similar (Table 6). Valve selection by listing the valves in order of preference (rank-preference)--as compared with a selection process in which the pilot compared the valve being tested with the F-15 valve (as he remembered it)--was significantly correlated ($P < 0.01$) with a correlation coefficient of 0.74 with 37 degrees of freedom. The hi-flow ready pressure valve was the only experimental valve unanimously preferred over the F-15 valve by the pilots.

Each pilot was asked to specify the valve (day) that he would like to have incorporated into the F-15. Of 8 pilots, 5 selected the hi-flow ready pressure valve; 2 pilots selected the same valve for their second choice; and the remaining pilot named this valve as his third preference. This selection process was completed by the pilots while they were still "blind" as to the valves used in the study.

The final questionnaire, mailed to all 8 of the F-15 pilots 3 weeks after the end of the study, was returned by 7 of the pilots. These subjects agreed that their valve of choice was the hi-flow ready-pressure anti-G valve. Of course, in this instance, their selection was with benefit of complete knowledge of the study and much knowledge of the results.

DISCUSSION

The importance of using F-15 pilots in this study cannot be overemphasized, for two reasons: (a) the G-valve and G-suit act directly on the body of the pilot, so that comfort or rather lack of comfort (pain) must be considered; and (b) no known evaluation criteria for G-valves and G-suits are based on physiologic requirements. Hence the anti-G system is in a unique category of personal equipment. All other support equipment can be objectively measured using physiologic parameters, so that the support necessary to sustain the pilot can be calculated and provided; but this is not possible with anti-G equipment. Moreover, since the anti-G equipment developed in the 1950s has (until now) been found adequate by pilots of high-performance aircraft, there has been little demand to develop a method for adequate evaluation of anti-G systems. Moreover, devising a method of evaluating anti-G systems is difficult until the equipment deficiencies and modifications have been identified.

Identification of the deficiency in the anti-G system--as that of G-valve function at rapid G onset rates--made possible the modifying of the anti-G valve to correct this deficiency. When these modifications were perfected, a method could be developed for testing these experimental valves and determining if the deficiencies had been eliminated. Of course, the only persons available who could evaluate these valve modifications experimentally, relative to their impact on the deficiency, had to be F-15 pilots. These pilots also were needed to determine if our valve modifications (such as ready pressure) would offer some difficulty to the pilot while flying the aircraft.

Since no objective criteria are known for measuring anti-G protection during ACMs, subjective evaluation became of great importance. However, the primary difficulty with obtaining such subjective data, maintaining an unbiased subject population, can only be possible when the principal investigator is blind regarding the order of valve testing and the pilots are as blind as possible (within the limits of human experimentation regulations) regarding: (a) experimental design; (b) valves to be tested; and (c) order of valve testing.

In our study, it was possible to maintain this total experiment blind to such an extent that the pilots were unaware (and quite surprised) that one of the valves they had tested was the valve presently in the F-15. This factor increased the credibility of the pilots' low rating of the F-15 valve in the study. It is emphasized here that, for all valve evaluation criteria (Tables 2, 4, 5, and 6), the current F-15 valve consistently received the poorest marks.

The primary difficul that we encountered in maintaining a subject blind involved the existence of ready pressure in some of the experimental anti-G valves. Ready pressure was easily identified by all of the pilots prior to G exposure. We circumvented this difficulty by testing three different types of ready pressure valves. Since only one ready-pressure valve was identified as being superior by the majority of the pilots (5 out of 8), it is clear that they were able to discriminate among the various types of ready-pressure valves being tested.

Several questions were asked of the pilots concerning their opinion on the use of ready pressure in the F-15. All pilots stated that: They liked the idea of ready pressure; it was comfortable at 1 G; and they would use ready pressure if it were in the F-15. All preferred an on/off switch for ready pressure, because they planned to use it only in the aerial combat arena.

Two different approaches were used in having the pilots select the valves which they considered to be superior and to be compatible with the F-15. The comparison of the test valve with their recollection of the F-15 valve, on a daily basis, was quite different from the evaluation of the various valves. (The pilots, of course, did not realize that one of the valves tested was the F-15 valve.) Although each approach was quite different, the order of valve selection was similar. This observation is further supported by the high correlation coefficient of 0.74 ($P < 0.01$), comparing these 2 types of valve selection. This approach gave additional support to their selection of the hi-flow ready-pressure valve.

The test plan was developed using every subjective and objective parameter that was conceivable while using pilots as experimental subjects. Those parameters which had significant differences between the F-15 and the hi-flow ready pressure valves were identified: (a) effort (Table 2); (b) suit pressure at 7 G (Table 4); (c) valve response at 1, 3, and 6 G/sec (Table 4); and (d) valve error scores at 1, 3, and 6 G/sec (Table 5). Correlation coefficients were determined for these parameters relative to the 2 methods of valve selection in Table 6. These correlation coefficients are listed in Table 3. Clearly the most significant relationships are within the subjective parameters, as might be expected; for valve selection has a subjective basis. Subjective valve response was highly correlated with both valve selection methods, especially at the higher G-onset rates (3 and 6 G/sec). Surprising, however, was the high correlation between effort scores and valve selection. Since the G-suit's main function was considered by the pilots as a method of supporting the M-1, these experimental valves reduced the amount of effort required by the pilot to tolerate high G Instituting the hi-flow ready-pressure valve in the F-15 should significantly reduce pilot fatigue during ACM.

Clearly, our approach in using a simulation of an ACM in this study was useful to the pilots in their evaluation of these anti-G valves. All of the pilots remarked that our ACM profile was a reasonable simulation of an F-15 ACM. Surely, this type of test-bed is required to evaluate anti-G equipment; for fighter pilots use the anti-G suit primarily to support the M-1 at high G (Table 1).

In summary, the pilots selected the hi-flow ready-pressure valve for installation in the F-15. Their choice was based primarily on subjective evaluations of G-suit support during the rapid G-onset rates and effort required to tolerate high G exposures. Their subjective evaluations appear to have objective support, as measured using valve error scores and maximum suit inflation rates.

velocity. However, numerous experiments show that this is not the case and that C_d and C_m show considerable variations from those just cited above. Even though no one has suggested a better alternative, the use of the Morison's equation gave rise to a great deal of discussion on what values of the two coefficients should be used. Furthermore, the importance of the viscosity effect has remained in doubt since the experimental evidence published over the said period has been quite inconclusive.

The drag and inertia coefficients obtained from a large number of field tests, as compiled by Wiegel [5], show extensive scatter whether they are plotted as a function of the Reynolds number or the so-called period parameter $U_m T/D$. The reasons for the observed scatter of the coefficients C_m and C_d remained largely unknown. The scatter was attributed to several reasons or combinations thereof such as the irregularity of the ocean waves, free surface effects, inadequacy of the average resistance coefficients to represent the actual variation of the nonlinear force, omission of some other important parameter which has not been incorporated into the analysis, the effect of ocean currents on separation, vortex formation, and hence on the forces acting on the cylinders, etc.

The most systematic evaluation of the Fourier-averaged drag and inertia coefficients has been made by Keulegan and Carpenter [6] through measurements on submerged horizontal cylinders and plates in the node of a standing wave, applying theoretically derived values for velocities and accelerations. Additional measurements have been made by Sarpkaya [7] of the in-line as well as transverse forces acting on cylinders and spheres in a sinusoidally oscillating fluid and it was found that the drag coefficient as well as the inertia coefficient for a strictly sinusoidally oscillating fluid (no mean velocity) is a function of $U_m T/D$ and that the effect of the Reynolds number is rather secondary and certainly obscured by the excellent correlation of the data with the period parameter $U_m T/D$.

On the basis of the above discussion, one would assume that Morison's equation would apply equally well to periodic flow with a mean velocity where $u = \bar{V} - U_m \cos\theta$ and that C_{d1} and C_{m1} will have constant, time-invariant, Fourier or least-squares averages. This, in turn, implies that C_{d1} and C_{m1} are independent of the associated flow phenomena. There is, however, no a priori assurance in the principles of fluid mechanics of theory of models that this is, in fact, the case. Thus the effect of the combination of a uniform current and harmonic oscillations on the time-average and oscillatory forces acting on circular cylinders will have to be re-examined and the limits of application of the Morison's equation be delineated.

It is a priori evident that both $u = -U_m \cos\theta$ and $u = \bar{V} - U_m \cos\theta$ yield the same acceleration du/dt. Thus, the force in-phase with the acceleration in Morison's equation remains unaffected by the presence of the mean flow. The results presented herein show that this is not the case. Furthermore, the use of the Morison's equation as

$$F = 0.5\rho C_d(\bar{V}-U_m\cos\theta)\left|\bar{V}-U_m\cos\theta\right| + C_m\rho\pi\frac{D^2}{4}\frac{du}{dt} \tag{2}$$

requires that the time-averaged drag force be calculated by increasing the force calculated from the steady flow by a factor $[1 + 0.5(U_m/\bar{V})^2]$. The results presented herein show that such an analysis appreciably underestimates the measured mean forces. It suffices to state that the fluid flow phenomena for bluff bodies are significantly affected by the combination of currents and harmonic oscillations and that the results for steady currents alone and oscillations alone cannot be combined to yield reliable estimates of forces due to both acting together.

The time-dependent forces per unit length in the present study are analyzed according to the following three-coefficient equation:

$$F = 0.5\bar{C}_d\rho D\bar{V}^2 + C_m\rho\pi\frac{D^2}{4}\frac{d}{dt}\left(-U_m\cos\frac{2\pi}{T}t\right) - 0.5\,C_d D U_m^2\rho\left|\cos\frac{2\pi}{T}t\right|\cos\frac{2\pi}{T}t \tag{3}$$

which may be written as,

$$\frac{F}{0.5\rho D\bar{V}^2} = \bar{C}_d + C_m\pi^2(U_mT/D)(D/\bar{V}T)^2\sin\frac{2\pi}{T}t - C_d(U_mT/D)^2(D/\bar{V}T)^2\left|\cos\frac{2\pi}{T}t\right|\cos\frac{2\pi}{T}t \tag{4}$$

in which C_m and C_d are given by their Fourier averages as

$$C_m = (2U_mT/\pi^3D)\int_0^{2\pi}(F\sin\theta)\,d\theta/(U_m^2D) \qquad (5)$$

and

$$C_d = -(3/4)\int_0^{2\pi}(F\cos\theta)\,d\theta/(U_m^2D)\ . \qquad (6)$$

Evidently, C_d, C_m, and $\bar{C}_d$ are functions of $\bar{V}T/D$ and U_mT/D or A/D. They may depend also on the Reynolds number which does not explicitly appear in the above expression because of the assumptions made in the formulation of the basic force equation.

In the foregoing, neither the coefficient $\bar{C}_d$ is assumed to be equal to the steady-state drag coefficient for a uniform flow at the constant velocity $\bar{V}$, nor C_m and C_d are assumed to be identical to those obtained for a strictly harmonic oscillation. In fact, the results show that $\bar{C}_d = C_d$ (steady) only for $U_m = 0$, and C_d and C_m are equal to those obtained for the harmonic oscillation only for $\bar{V}T/D=0$.

The equation proposed above is general enough to be applicable to both in-line and transverse oscillations. In the case of transverse oscillations, however, the mean net force in the direction of oscillation is zero, i.e., $\bar{C}_d=0$, and thus, one has

$$\frac{\text{F(transverse force)}}{0.5\rho\bar{D}V^2L} = C_{m1}\pi^2(U_mT/D\ D/\bar{V}T)^2\sin\frac{2\pi}{T}t -$$

$$C_{d1}(U_mT/D)^2(D/\bar{V}T)^2\left|\cos\frac{2\pi}{T}t\right|\cos\frac{2\pi}{T}t \qquad (7)$$

In the foregoing, the inertia and drag coefficients have been denoted as C_{ml} and C_{dl} in order to distinguish them from those corresponding to in-line oscillations. The subscript "l" carries the meaning of "Lift" or force in the direction transverse to the stream.

Ordinarily, for a perfectly sinusoidal oscillation of the cylinder, the coefficients C_{ml} and C_{dl} would be given by equations (5) and (6). However, when the oscillations are not perfectly harmonic, it is relatively more accurate to use the velocities and the accelerations encountered in the experments rather than assuming them to be simple harmonic motions. It is with this objective in mind that the equations (5) and (6) were rewritten as

$$C_{ml} = \frac{2T}{\pi^2 D^2 \rho U_m} \frac{\int_0^{2\pi} F \sin\theta \, d\theta}{\int_0^{2\pi} \sin^2\theta \, d\theta} \tag{8}$$

and

$$C_{dl} = \frac{-T^2}{2\rho D \pi^2 A^2} \frac{\int_0^{2\pi} F \cos\theta \, d\theta}{\int_0^{2\pi} \cos^2\theta \left|\cos\theta\right| d\theta} \tag{9}$$

as they would be obtained from equation (3) in the usual application of the Fourier analysis. Evidently, had the oscillations been perfectly harmonic the integrals appearing in the denominators of the above equations would have reduced to

$$\int_0^{2\pi} \sin^2\theta \, d\theta = \pi \tag{10}$$

and

$$\int_0^{2\pi} \cos^2\theta \left|\cos\theta\right| d\theta = \frac{8}{3} \tag{11}$$

The equations (8) and (9) together with equations (10) and (11) would have reduced to equations (5) and (6).

Since in the present study the periodic oscillations were not perfectly sinusoidal partly by design in order to obtain greater generality and flexibility in the experimentation and partly due to the constraints imposed in the design of the oscillation mechanism, it became necessary to incorporate into equations (8) and (9) the exact form of the oscillations encountered in the experiments. For this purpose the dry force, which is an exact representation of the oscillations, was normalized as

$$f(\theta) = \frac{\text{F-dry}}{\left|\text{F-dry (maximum)}\right|} \tag{12}$$

and then the equations (8) and (9) were rewritten as

$$C_{ml} = \frac{2T}{\rho\pi^2 D^2 U_m} \frac{\int_0^{2\pi} F\cdot f(\theta)\, d\theta}{\int_0^{2\pi} f(\theta)\, d\theta} \tag{13}$$

and

$$C_{dl} = -\frac{T^2}{2\rho D\pi^2 A^2} \frac{\int_0^{2\pi} F\cdot f(\theta+\pi/2)\, d\theta}{\int_0^{2\pi} f(\theta + \pi/2)\left|f(\theta + \pi/2)\right| d\theta} \tag{14}$$

It is easy to show that equations (13) and (14) reduce to equations (8) and (9) or to equations (5) or (6) for purely harmonic oscillations for which f(θ) = sin θ. The advantage of the use of the equations (13) and (14) is rather obvious for all types of periodic oscillations. Furthermore, the independent evaluation of the denominators of equations (13) and (14) and their comparison with π and 8/3 respectively (as they would have been equal to had the oscillations been harmonic) gave an indication of the deviation of the observed periodic oscillations from a purely harmonic oscillation.

The force acting on the cylinder in the in-line direction due to the oscillations in the transverse direction is expressed in terms of a mean drag coefficient, denoted by $\bar{C}_{di}$, given as

$$\bar{C}_{di} = \frac{\text{Force in the in-line direction}}{0.5\ \rho L D \bar{V}^{2}} \tag{15}$$

Evidently, $\bar{C}_{di}$, C_{m1} and C_{d1} depend on $D/\bar{V}T$, $U_m T/D$ or $2\pi A/D$, and possibly on the Reynolds number.

The experimental data are analyzed using the computer program given in Appendix A written according to the equations (13), (14), and (15) and are plotted in terms of A/D and either $D/\bar{V}T$ or $\bar{V}T/D$.

IV. DISCUSSION OF RESULTS

The results will be discussed in two parts. The first will be the average in-line force acting on the cylinder undergoing forced periodic oscillations in the transverse direction. The second will be the time dependent transverse force.

Evidently, the average in-line force is coupled with secondary oscillations due to vortex shedding. However, such oscillations are rather small in both steady and periodic flows and certainly not larger than about seven percent of the average force. It is for this reason that only the average of the in-line force acting on the oscillating cylinder is presented here.

Figures 5, 6, and 7 show the variation of the normalized in-line force ($\overline{C}_{di}$) as a function of $D/\overline{V}T$ for $A/D = 0.25$, 0.50 and 0.75 respectively. Each figure represents the data obtained with two velocities, namely, $\overline{V}=0.84$ and $\overline{V}=1.3$. In normalized form these velocities correspond, for the one inch cylinder used, to the Reynolds numbers $Re=\overline{V}D/\nu = 7000$, and $Re = 10,833$.

Evidently, the in-line force increases with A/D since the cylinder, undergoing transverse oscillations, presents a larger apparent-projected area to the mean flow. This, however, is only part of the explanation. In addition, the vortex growth and motion are affected by the oscillation of the cylinder which in turn affect the in-line and transverse forces acting on the cylinder. This is evidenced by the fact that the in-line force for a given A/D increases at first, reaches a maximum, and then decreases as $D/\bar{V}T$ increases. A simple minded calculation based on the steady flow drag coefficient for a stationary cylinder and the apparent projected area for the in-line force $\bar{F}$, which may be written as

$$\bar{C}_{di} = \frac{\bar{F}}{\frac{1}{2}\rho\bar{V}^2 DL} = C_{ds}(1 + 2A/D)$$

yields values which are almost equal to the maximum values given in figures 5, 6, and 7. It should be noted, however, that the phenomenon is far more complex, and that such a simple minded procedure should not generally be used, even though the results are surprisingly good.

For the purposes of comparison, the figures 5, 6, and 7 are combined in figure 8 by drawing mean lines through the data points. Figure 8 shows that the in-line force coefficient reaches its maximum at $D/\bar{V}T$ between 0.18 and 0.20. Ordinarily, the Strouhal number for a stationary cylinder would be 0.22 for the Reynolds numbers cited previously, and one would expect that the forces acting on the cylinder will undergo dramatic changes as the vortex shedding frequency given by the Strouhal number coincides with the frequency of the cylinder oscillations. The present results show that such a synchronization takes place at a frequency slightly lower than the Strouhal frequency.

Also shown in figure 8 is the normalized amplitude of the oscillations in the in-line force for A/D=0.5. As noted earlier the oscillations are quite negligible relative to the mean force and certainly under 7 per-cent. It should be noted that the amplitude of the oscillations, like the average force show an almost sudden increase in the vicinity of $D/\bar{V}T$ nearly equal 0.19 and remain at that value for larger values of $D/\bar{V}T$. The occurrence of synchronization as well as the increase of the amplitude of oscillations are shown most dramatically in figure 9. This figure was obtained by setting the free stream velocity at 0.84 feet per second and the A/D ratio equal to 0.5. Then, beginning with the case of the non-oscillating cylinder, the frequency of the oscillations was gradually increased up to about four cycles per second and the resulting in-line force was continuously recorded. The figure shows that the in-line force increases rapidly but with very little oscillations superimposed on it. As soon as the state of synchronization is reached, the amplitude as well as the frequency of the force oscillations increases.

From an engineering point of view the significance of the magnitude of the in-line force is that a cylinder or cable excited by the flow to oscillate in the transverse direction may be subjected to in-line forces several times larger than assumed in its design. Furthermore, the deflections caused by the in-line force on sufficiently flexible cylinders tend to couple with transverse ocsillations and not only affect the magnitude of the transverse oscillations but also the path of the cylinder motion. Thus it is not uncommon to see heat exchanger pipes or chimneys exhibit oscillation patterns in the plane normal to their axis.

The time dependent transverse force is described, as noted earlier, in terms of a drag coefficient C_{d1} and an inertia coefficient C_{m1} given by

$$\text{Transverse Force} = C_{m1}\frac{\rho\pi D^2}{4}\frac{d}{dt}(-U_m\cos\theta) - C_{d1}\frac{\rho D U_m^2}{2}\cos\theta\left|\cos\theta\right|$$

Figures 10, 11, and 12 show C_{d1} and C_{m1} as a function of $\bar{V}T/D$ for A/D - 0.25, 0.50 and 0.75 respectively. These coefficients were obtained without the $\tilde{C}_d$ term in the generalized Morison equation (see equation 3) since the mean of the transverse force is zero.

It is seen from these figures that important variations in C_{d1} and C_{m1} occur particularly in the vicinity of the Strouhal frequency (here $\bar{V}T/D \cong 4.5$ to 7) where the natural eddy-shedding at the Strouhal frequency is both enhanced and correlated by the oscillations.

The inertia coefficient or the normalized in-phase component of the transverse force undergoes a rapid drop as the frequency of the oscillation approaches the Strouhal frequency from both the upper and lower limits. In other words, synchronization or lock-in is manifested by a rapid decrease in inertial force and a rapid increase in the absolute value of the drag force.

This fact, which has not been recognized before, shows that the lock-in phenomenon is a phase transformer. The total force which is nearly in phase with the motion before synchronization becomes nearly out of phase at or after synchronization. It should be noted in passing that the success of many empirical models dealing with this type of oscillation comes partly from the manipulation of the phase angle near synchronization. It is now apparent that the fluid force to be used in the equations expressing the motion of a cylinder or cable should be given by the data presented herein. Such data take care, not only of the variation of the phase angle, but also the amplitude of the transverse force as a function of the normalized frequency.

Figure 13 depicts an example of the occurrence of synchronization as the period of oscillation is gradually decreased. The upper trace shows the phenomenon as frequency is increased from the non-oscillating case to just beyond the synchronization frequency. The lower trace provides an exploded view of the phenomenon near the synchronization frequency. The transverse force sharply decreases as soon as the point of synchronization is reached. Beyond that point, the changes in the transverse force with frequency are quite small. The phenomenon is reversible and one would obtain a figure similar to figure 13 if one gradually decreased the frequency. The possibility of the occurrence of an hysteresis has not been investigated.

The drag coefficient C_{dl} or the normalized out-of-phase component of the total instantaneous transverse force given in figures 10, 11, and 12 show that C_{dl} becomes negative for $\bar{V}T/D$ values between approximately 4.5 and 7 (i.e. for normalized frequencies between 0.14 and 0.22). Outside this range the drag is positive, thus in the opposite direction to the motion of the cylinder. Within the range of $\bar{V}T/D$ values cited above, the drag force is in phase with the direction of motion of the cylinder and helps to magnify the oscillations rather than damp them out. For this reason, the region in which C_{dl} is negative is sometimes referred to as the negative damping region. The fact of the matter is that this is not damping in the proper use of the word but rather an energy transfer from the fluid to the cylinder via the mechanism of synchronization. The values of $\bar{V}T/D$ at which C_{dl} changes its sign depend on A/D as seen in figures 10, 11, and 12 even though the negative C_{dl} region roughly lies within the $\bar{V}T/D$ values of 4.5 and 7. The envelopes of the two zero crossings determine the region of self excited oscillations. A precise determination of such an envelope and its dependence on the Reynolds number will require experiments with additional A/D values and other Reynolds numbers. For purposes of the present investigation, it suffices to note that the maximum absolute value of C_{dl} in the synchronization range decreases rapidly as A/D increases. Field studies have shown that synchronization does not occur for relative amplitudes larger than unity. The trend of the present data is in conformity with such observations.

Finally, an unexpected and previously unknown observation in connection with the variation of C_{dl} will be described. For normalized frequencies ($D/\bar{V}T$) smaller than about 0.14 the data yield positive drag coefficients. Between 0.14 and 0.22, the synchronization takes place and the drag coefficient is negative as noted above. Ordinarily one would have expected that the drag coefficient will remain positive and continue to increase with increasing frequencies beyond $D/\bar{V}T = 0.22$ and eventually reach a value which would be identical to that obtained by oscillating the cylinder in a fluid otherwise at rest. However, an interesting phenomenon takes place at frequencies between approximately 0.22 and 0.27, depending on the A/D ratio. For example for $A/D=0.75$, C_{dl} increases rapidly at $D/\bar{V}T = 0.26$ and then decreases sharply to nearly zero. At first it was suspected that this might be due to an experimental error. However, the repeatability of the experiments and the observation of the same phenomenon at other relative amplitudes and velocities have shown that there is indeed a dramatic change in C_{dl} at $D/\bar{V}T = 0.26$ for $A/D = 0.75$, at $D/\bar{V}T = 0.24$ for $A/D = 0.5$ and at $D/\bar{V}T = 0.31$ for $A/D = 0.25$. It is further noted from the data presented herein that, particularly for $A/D = 0.25$ and 0.5, C_{dl} becomes once more negative in a narrower range of $D/\bar{V}T$ values ($D/\bar{V}T$ from 0.345 to 0.45 for $A/D = 0.25$) and shows the existence of a second region of synchronization. The $D/\bar{V}T$ value at which C_{dl} acquires its second minimum value in the case of $A/D = 0.25$ is almost exactly twice that of the first minimum. The occurrence of this second region of synchronization at higher frequencies depends on the A/D value. The fact that is demonstrated here is that there is not a single region of synchronization and that there is at least one and possibly more regions of frequency in which synchronization can occur.

The narrowness of the regions of frequency in the second region of synchronization makes it rather difficult to observe the phenomenon. In fact one may easily miss such a region by simply not taking smaller increments in frequency. It suffices to say that a cylinder may be excited first at frequencies near the Strouhal frequency and then at the multiples of the Strouhal frequency. However, the largest energy transfer from the fluid to the cylinder occurs in the first synchronization region near the Strouhal frequency.

V. CONCLUSIONS

The experimental investigation of the transverse oscillations of a cylinder in a flow with an ambient mean velocity $\bar{V}$ has yielded the force coefficients $\bar{C}_{di}$, C_{ml} and C_{dl} and has shown that;

a. The mean flow has significant effects on the force transfer coefficients and that the result of experiments with harmonic oscillations in a fluid otherwise at rest are not applicable to the transverse oscillations of a cylinder in a uniform flow;

b. It is possible to excite transverse oscillations for A/D smaller than about unity. In a region of $D/\bar{V}T$ in the vicinity of the Strouhal frequency, the out-of-phase component of the total force becomes negative and some energy is transferred from the fluid to the cylinder. The rate at which energy is transferred decreases with increasing relative amplitudes. Furthermore there is at least one, and possibly more, narrower bands of frequencies at which synchronization occurs.

c. Transverse oscillations give rise to an increased drag force in the direction of the mean flow. This force depends on A/D as well as on $D/\bar{V}T$ and reaches a maximum at about $D/\bar{V}T = 0.18$. This value corresponds to a normalized frequency slightly below the Strouhal frequency and is well within the synchronization region. From a practical point of view this is a matter of concern for sound design of cables and other structures which may be subjected to transverse oscillations;

d. A meaningful dynamic analysis of the vortex excited transverse oscillations may be carried out only through the use of the force transfer coefficients presented herein. However, it is necessary that additional data be obtained at other amplitudes and Reynolds numbers. Furthermore, additional experiments may also have to consider the roughness of the oscillating structure.

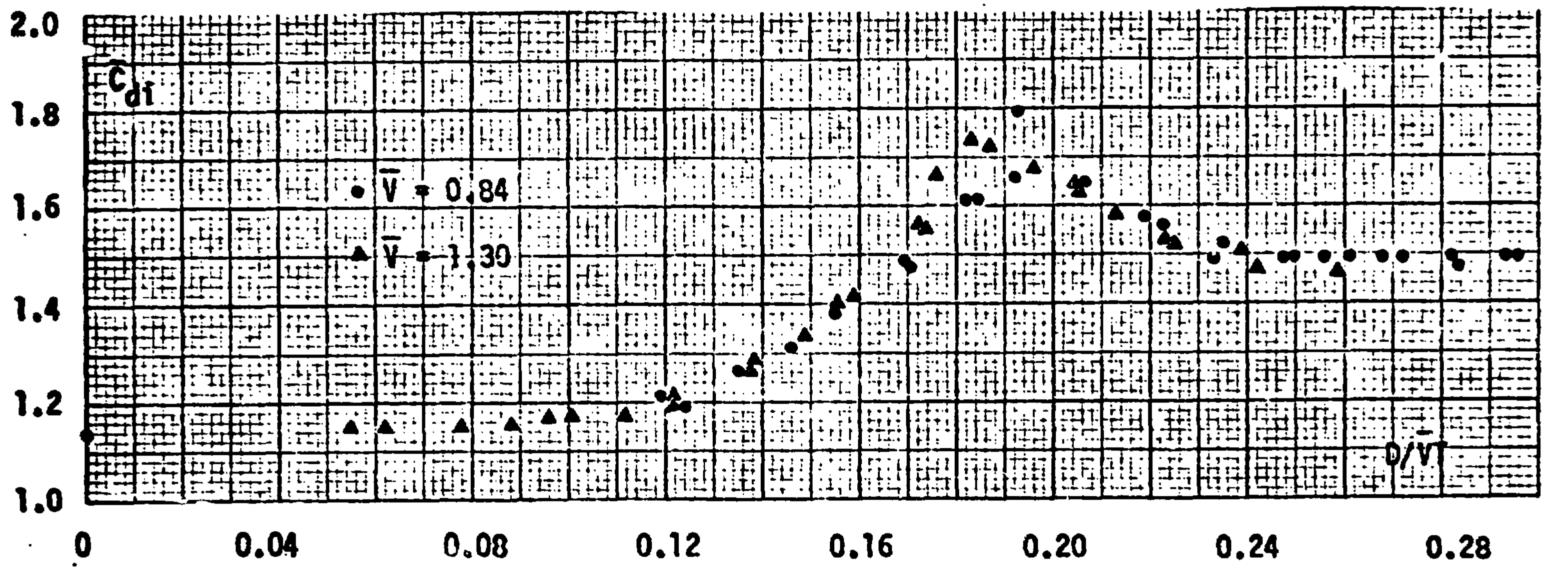

Fig. 5 Mean in-line drag coefficient versus $D/\bar{V}T$ for $A/D = 0.25$

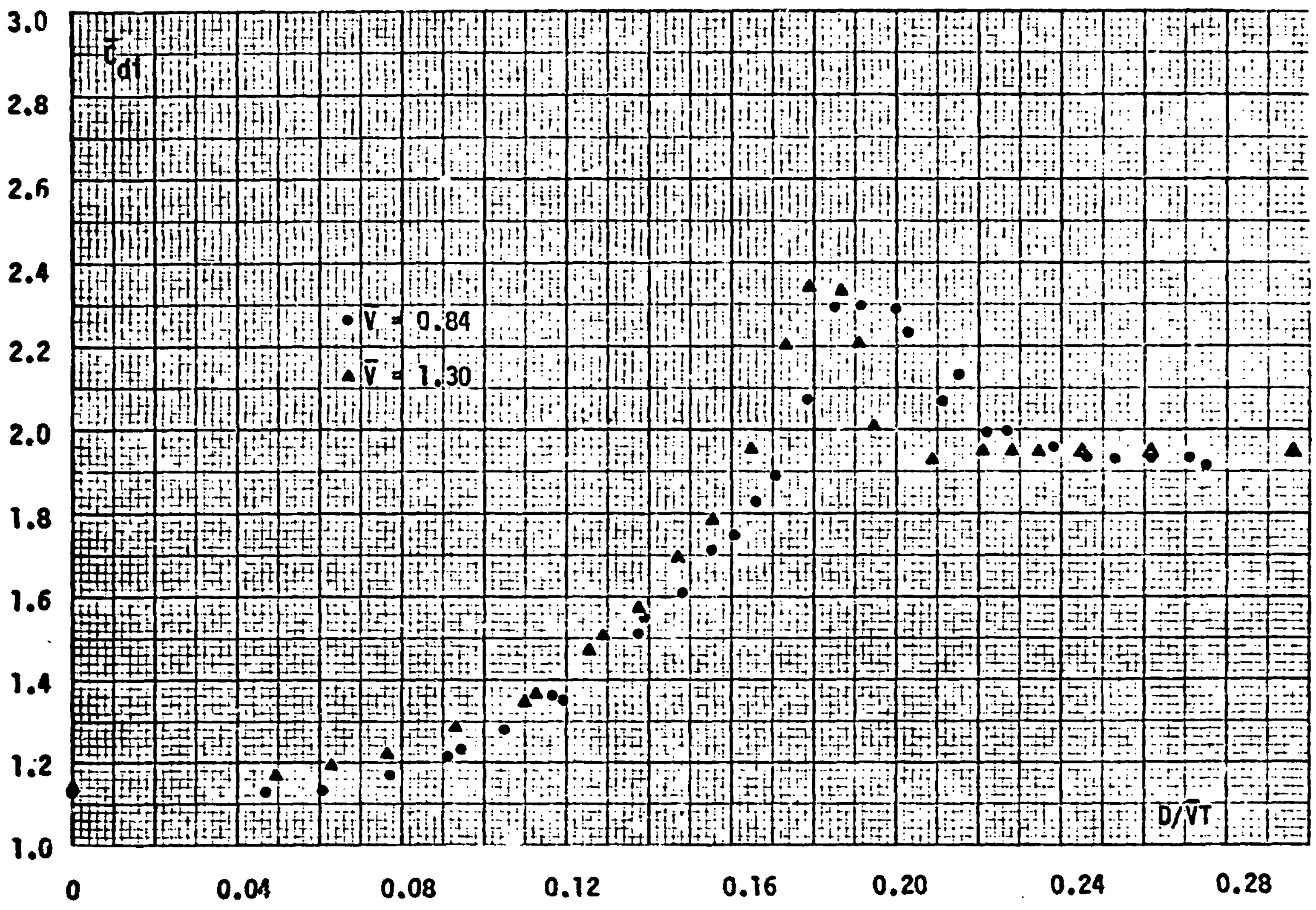

Fig. 6 Mean in-line drag coefficient versus $D/\sqrt{\bar{V}T}$ for $\Lambda/D = 0.50$

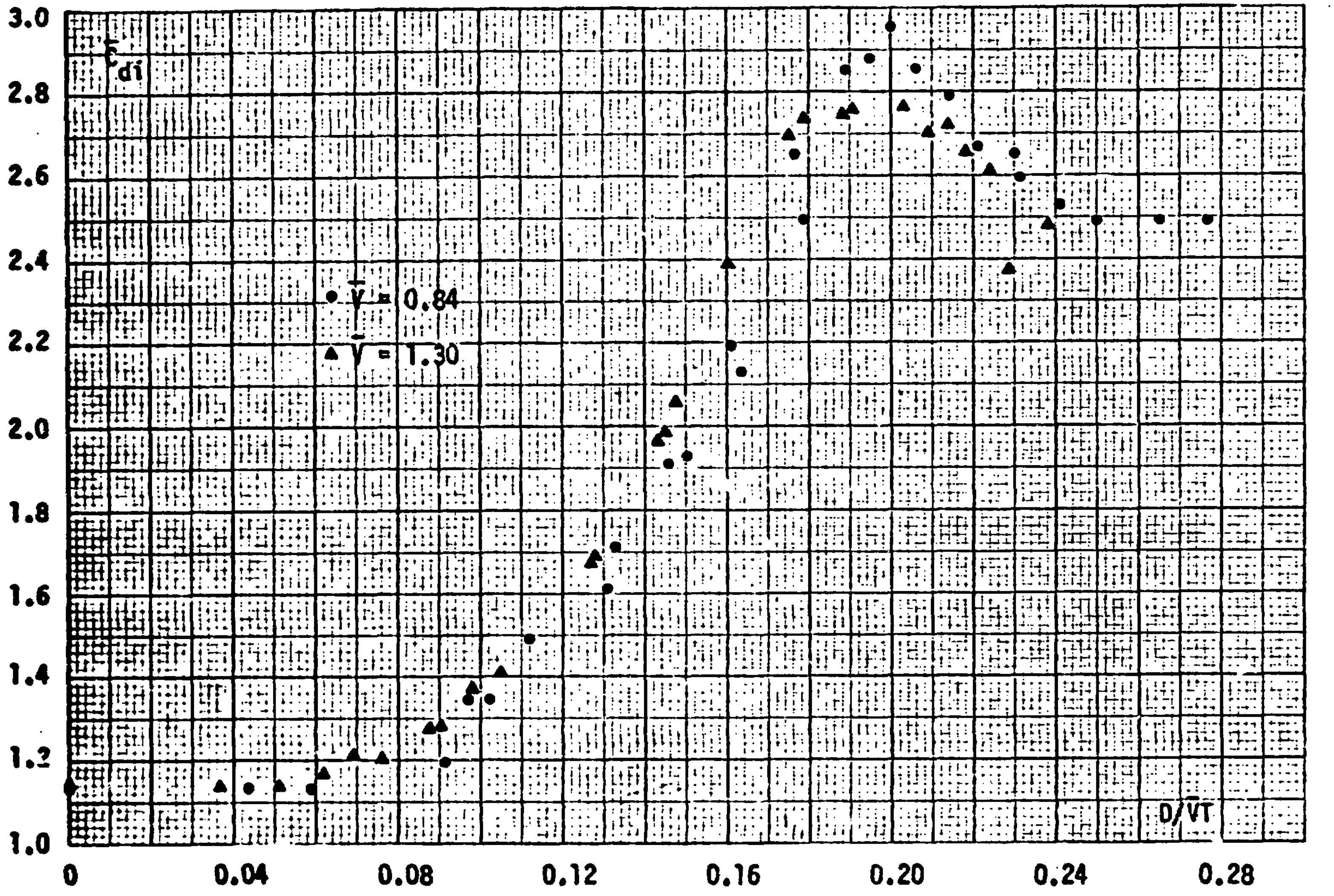

Fig. 7 Mean in-line drag coefficient versus $D/\bar{V}T$ for A/D = 0.75

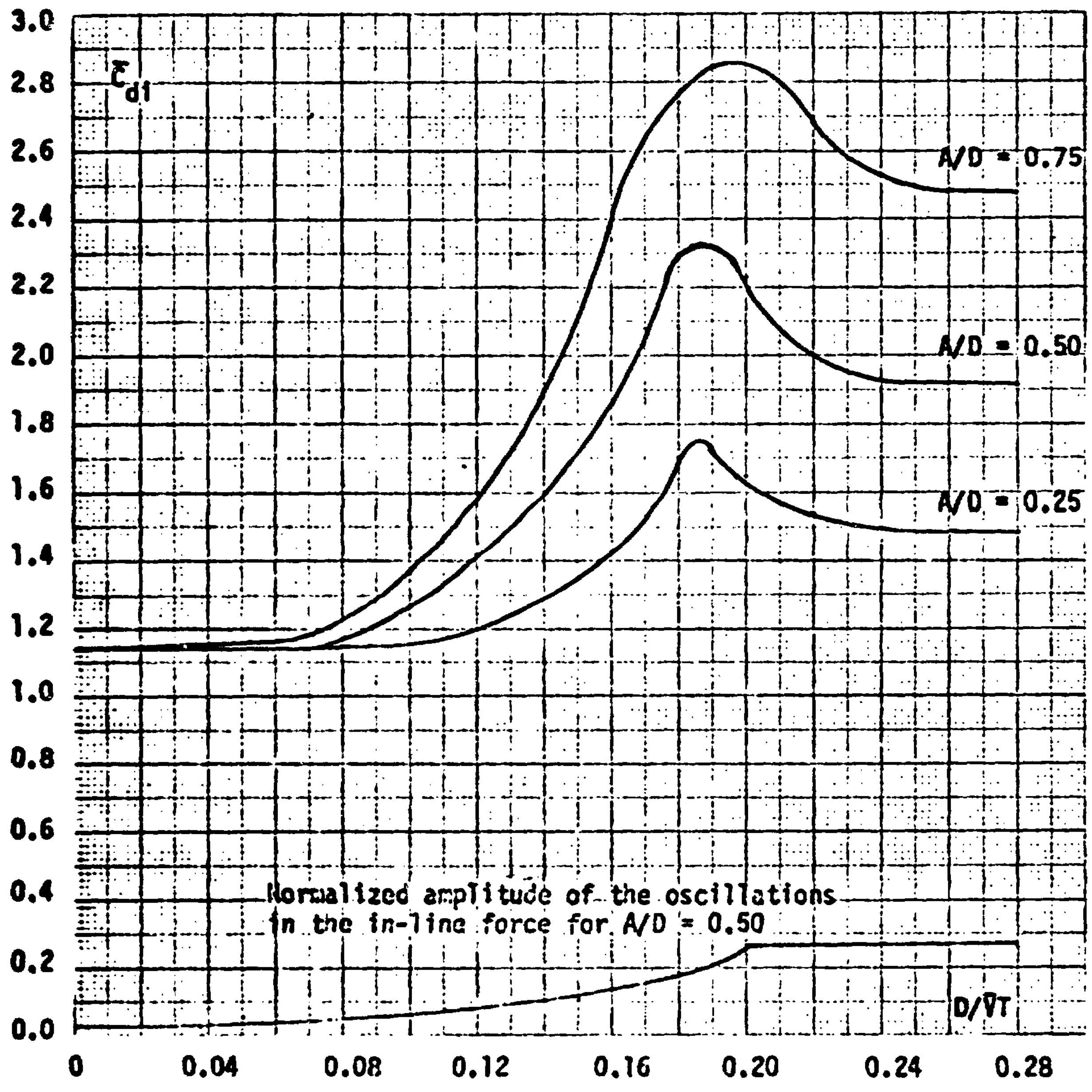

Fig. 8 Comparison curves: Mean in-line drag coefficient versus D/$\bar{V}$T for A/D = 0.25, 0.50, 0.75

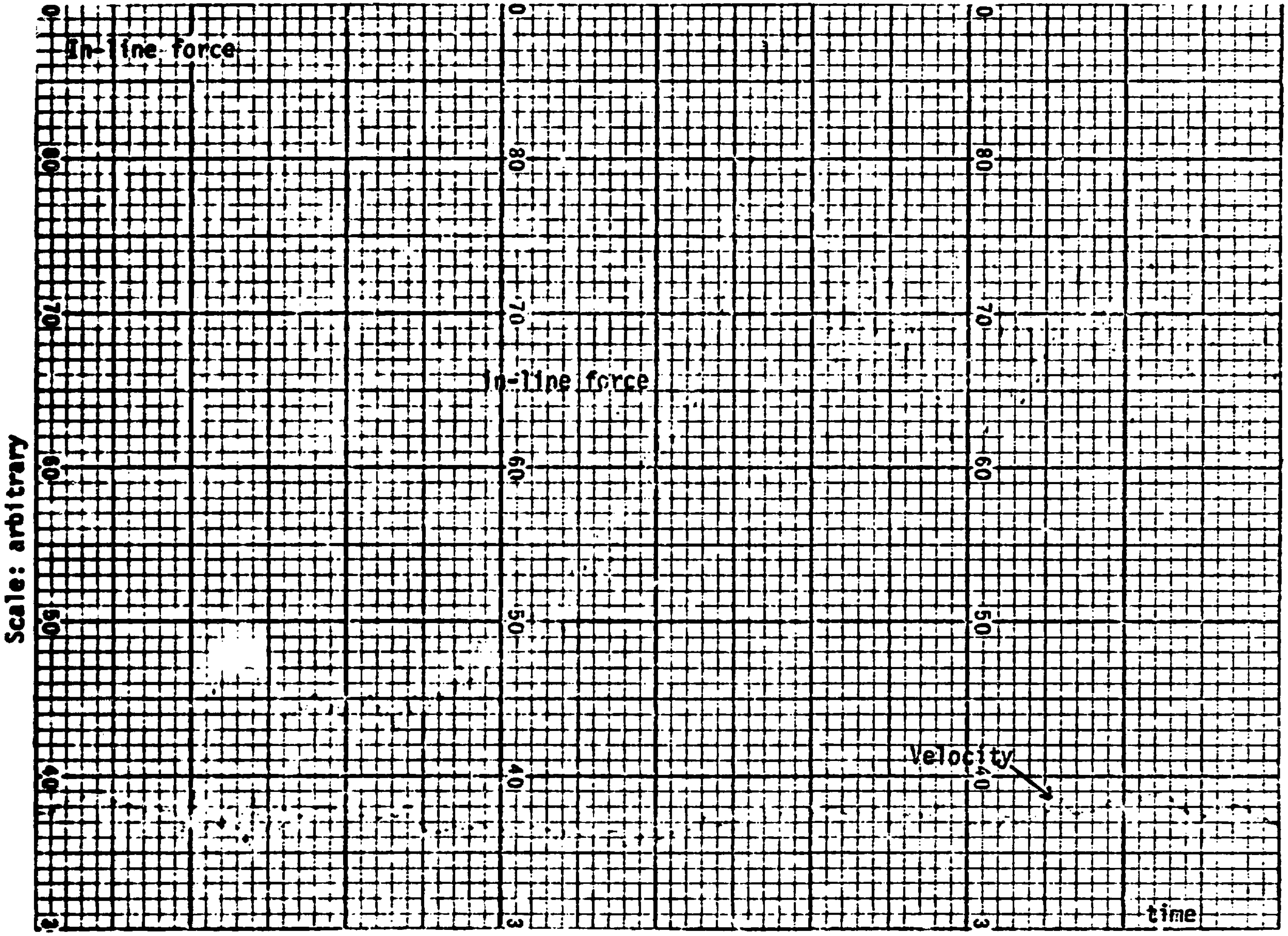

Fig. 9 Phenomenological Demonstration: In-line force versus time while increasing oscillation frequency, ($\Lambda/D = 0.50$, $V = 0.84$, $D/\overline{V}T = 0$ to 0.31).

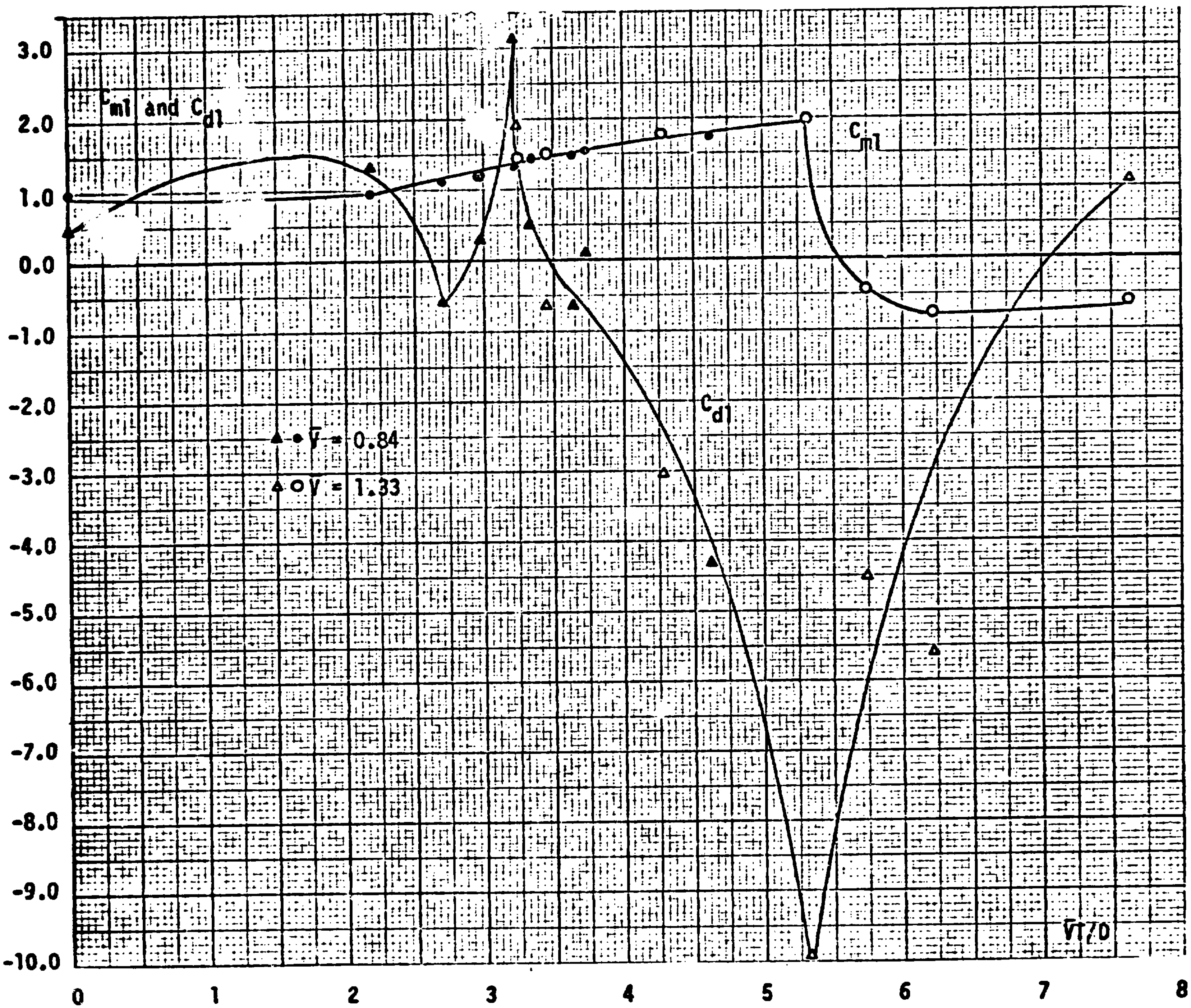

Fig. 10 Fourier-averaged drag and inertia coefficients C_{d1} and C_{m1} versus $\overline{V}T/D$ for $A/D = 0.25$.

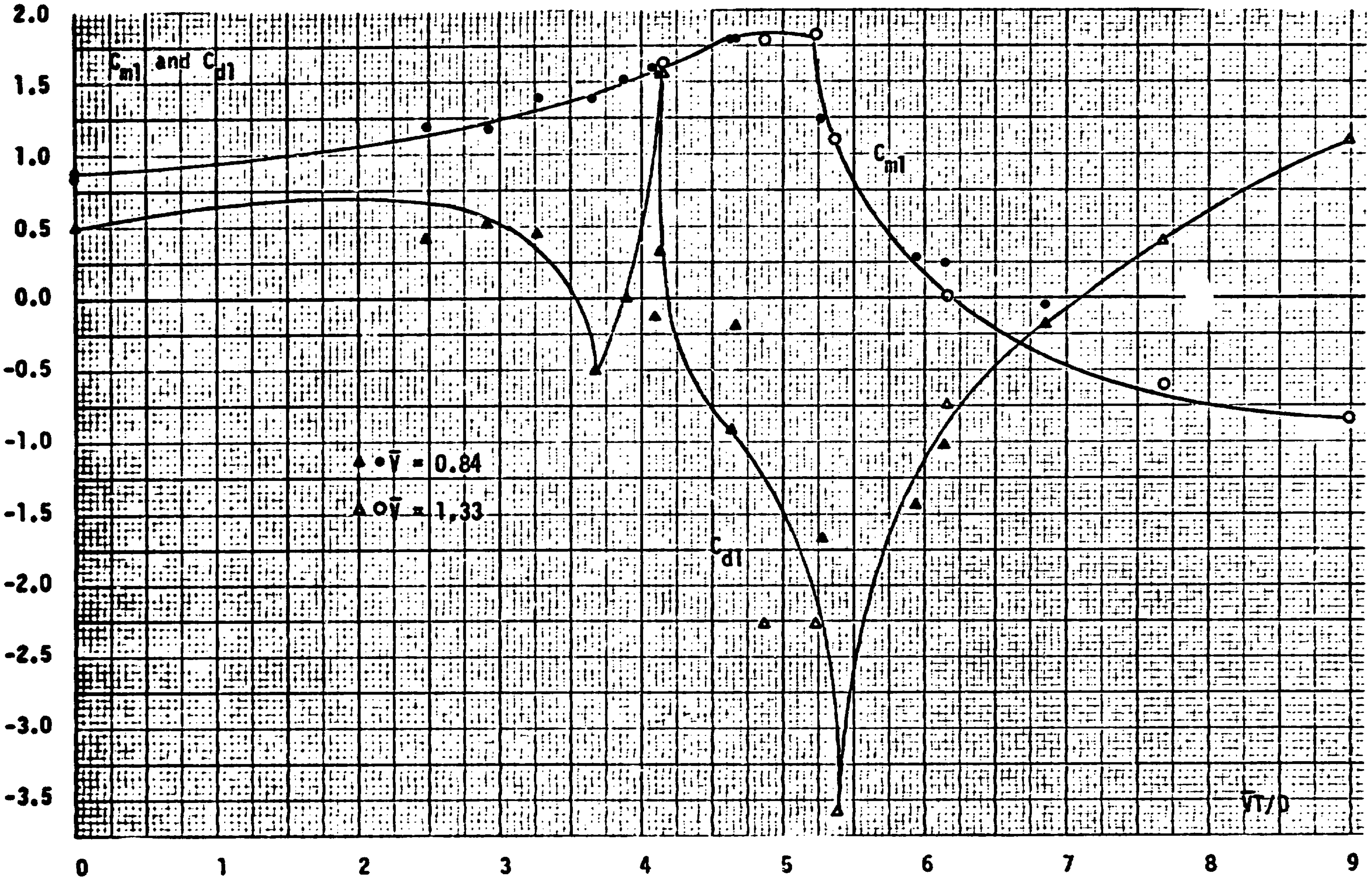

Fig. 11 Fourier-averaged drag and inertia coefficients C_{d1} and C_{m1} versus $\overline{V}T/D$ for $A/D = 0.50$.

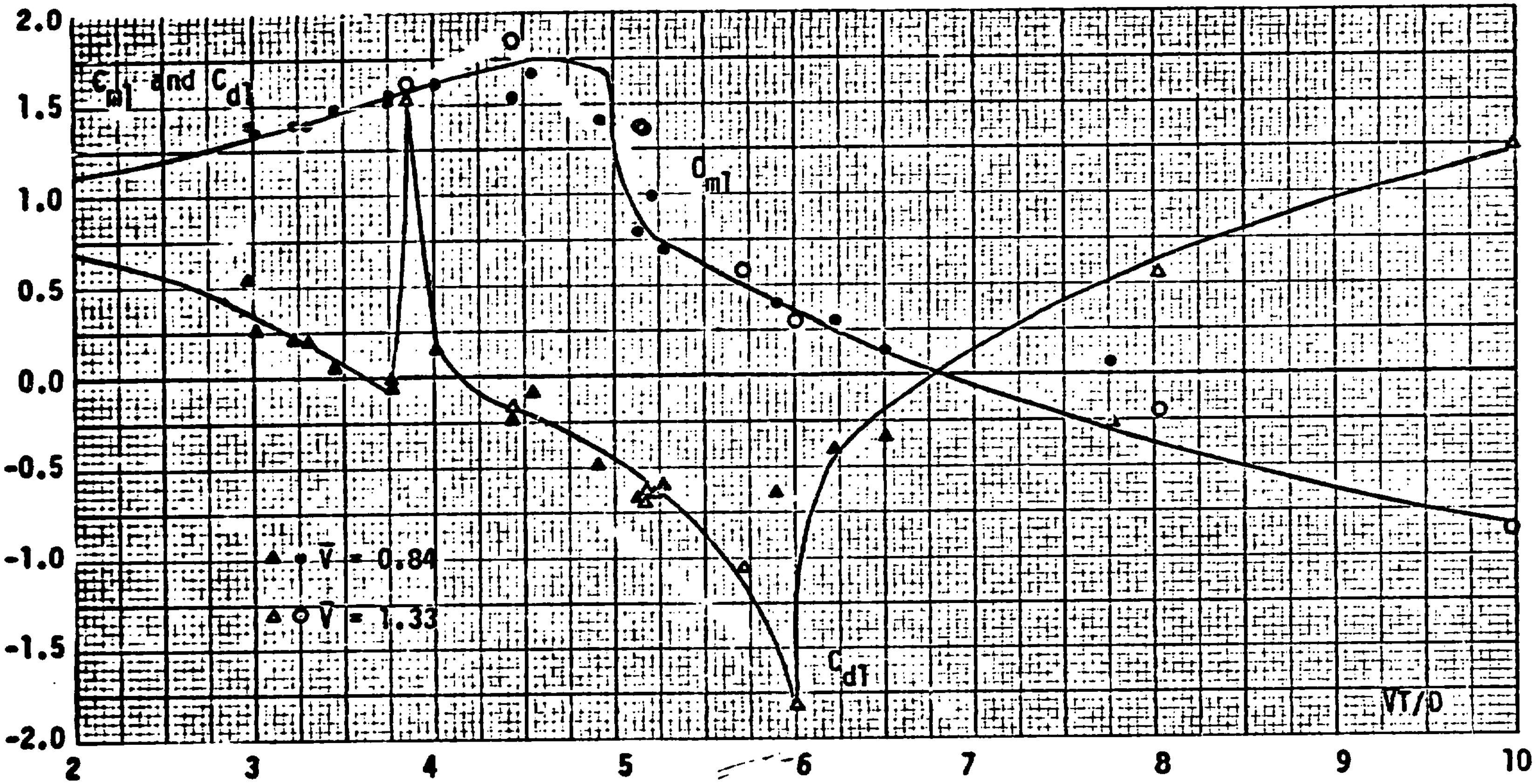

Fig. 12 Fourier-averaged drag and inertia coefficients C_{d1} and C_{m1} versus $\bar{V}T/D$ for A/D = 0.75.

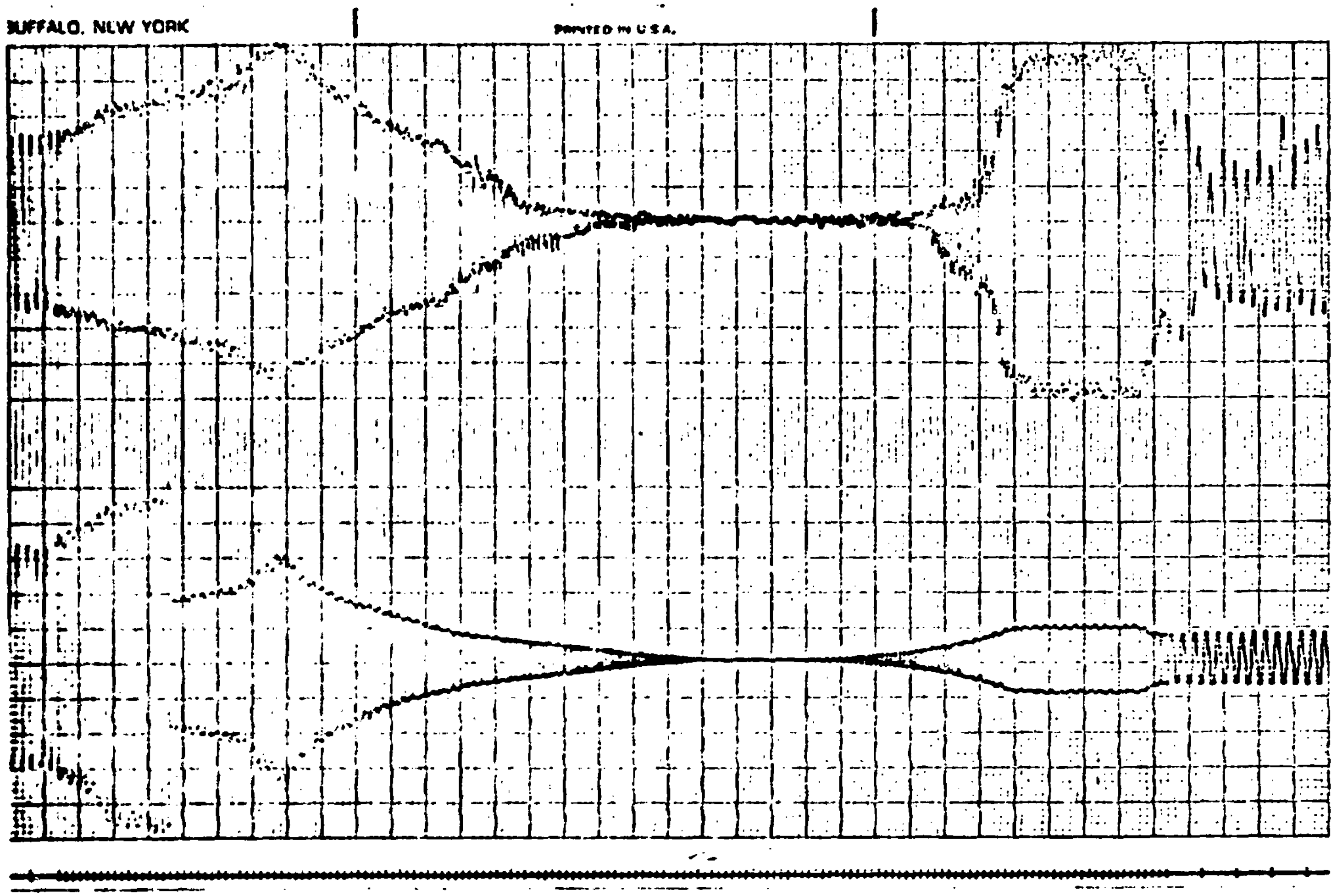

Fig. 13a Phenomenological demonstration: Transverse force versus time while increasing the oscillation frequency

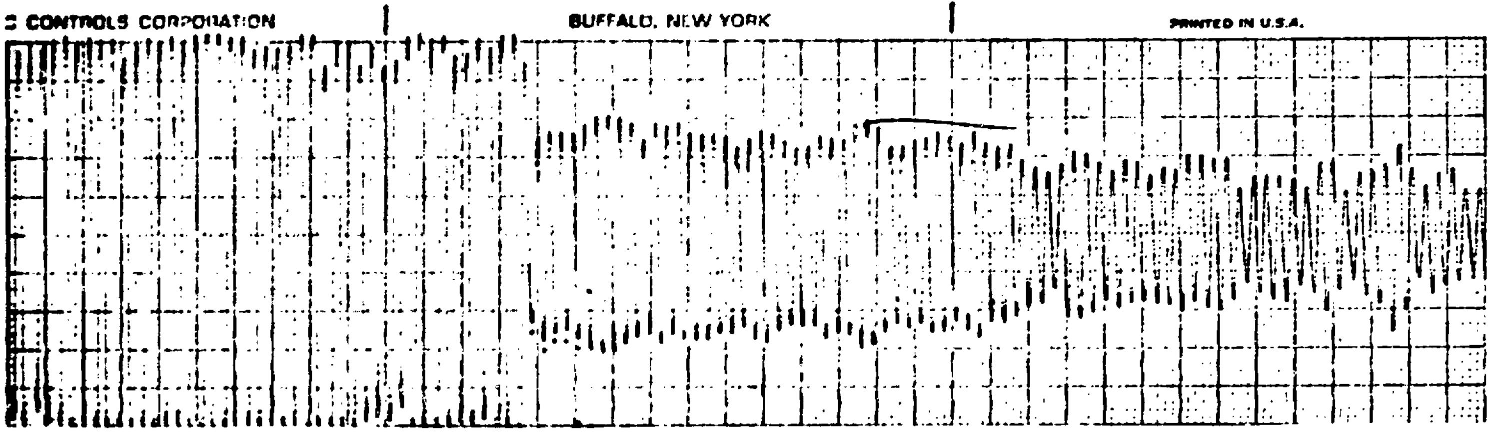

Fig. 13b Phenomenological demonstration: Transverse force versus time while increasing the oscillation frequency

APPENDIX A
COMPUTER PROGRAM

```
CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C                                                           C
C     [illegible] CALCULATIONS FOR CYLINDERS                C
C     TIME=NONDIMENSIONAL TIME (TIME/PERIOD)                C
C     BETA=NONDIMENSIONAL DISPLACEMENT(UMAX*PER/DIA)        C
C     VNT=[illegible] FREQUENCY PARAMETER                   C
C     CL=CYLINDER LENGTH IN FEET                            C
C     DIA=DIAMETER OF CYLINDER IN FEET                      C
C     AMP=AMPLITUDE OF MOTION IN FEET                       C
C     PER=PERIOD IN SECS (CHART PERIOD/CHARTSPEED)          C
C     ZM1 [illegible] COEFFICIENTS                          C
C     FNET=NET FORCE IN CHART UNITS                         C
C     FDRY=DRY FORCE IN CHART UNITS                         C
C     FMAX=MAXIMUM DRY FORCE IN CHART UNITS                 C
C     FNMEAN=AVERAGE NET FORCE IN CHART UNITS               C
C     FDMEAN=AVERAGE DRY FORCE IN CHART UNITS               C
C     CM=INERTIA COEFFICIENT                                C
C     CD=DRAG COEFFICIENT                                   C
C     NSET=NUMBER OF DATA SETS                              C
C     CN=CONVERSION FACTOR                                  C
C                                                           C
CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
      DIMENSION FORCE(300),FNET(300),FMAX(20),SINA(300),COSA(300)
C     READ THE PARAMETERS WHICH CHARACTERIZE THE DATA SET
C
      I=1
      DO [illegible] I=1,[illegible]
      READ(5,1)DIA,AMP,PER,NCARD,UMX,CN,FNMAX,NRUN,NDRY
      CYCLE=[illegible].
      NMAX=2.*CYCLE+1
      READ(5,55)(FMAX(K),K=1,NMAX)
      READ(5,55)(FNET(K),K=1,NCARD)
      READ(5,55)(FORCE(K),K=1,NDRY)
      G=32.174
      RHO=62.4/G
      PI=3.14159
      [illegible]1.E-5
      CL=.[illegible]
      VFACT=1.[illegible]
      X=([illegible])/(4.0*CYCLE))+0.499
      Y=[illegible]/(2.0*CYCLE)
      [illegible] SETS
C
C
C     COMPUTE FORCE COEFFICIENTS
      ZM1=(2.[illegible]*CN)/(RHO*PI*DIA*DIA*CL*PI*PI*AMP)
      ZM2=-([illegible]*PER*PER*CN)/(RHO*DIA*PI*CL*AMP*AMP)
      [illegible]
      BETA=[illegible]/DIA
      REYN=(VMAX*DIA)/CNU
      WRITE(6,15)DIA,AMP,PER,UMX,CN,NRUN
      WRITE(6,20)
      FNMEAN=0.0
      FDMEAN=0.0
      DO 15 K=1,NCARD
      FNMEAN=FNMEAN+FNET(K)
      FDMEAN=FDMEAN+FORCE(K)
   15 CONTINUE
      FNMEAN=FNMEAN/NCARD
      FDMEAN=FDMEAN/NCARD
      DO 94 K=1,NDRY
      FORCE(K)=-(FORCE(K)-FDMEAN)
   94 CONTINUE
      DO 97 K=1,NCARD
      FNET(K)=-(FNET(K)-FNMEAN)
   97 CONTINUE
      DO [illegible] K=1,NMAX
      FMAX(K)=ABS(FMAX(K)-FDMEAN)
   94 CONTINUE
      [illegible]=0.0
      STEP=[illegible]/(VMAX*PER)
      VMAX1 = VMAX*PER/DIA
      TIME=0.0
```

```
      CM=0.0
      CD=0.0
      CMA=0.0
      CDW=0.0
      [illegible]=0.0
      C[illegible]=0.0
      CA[illegible]=0.0
      DELTAT=CYCLE/NCARD
C
C     COMPUTE SINE FUNCTION
      FLIM=[illegible]Y-1
      DO 93 K=1,FLIM
      F=(FORCE(K)+FORCE(K+1))/2.0
      DO 95 L=1,NMAX
      IF(K.GT.(Y*(L-1)).AND.K.LE.(Y*L))FMMM=FMAX(L)
   95 CONTINUE
      SINA(K)=F/FMMM
   93 CONTINUE
      TIME=TIME+DELTAT/2.0
      J=[illegible]-1
C     VALUES OF INERTIA AND DRAG COEFFICIENTS
C
      DO 200 K=1,J
      DO 96 L=1,NMAX
      IF(K.GT.(Y*(L-1)).AND.K.LE.(Y*L))FMMM=FMAX(L)
   96 CONTINUE
      F=(FORCE(K)+FORCE(K+1))/2.0
      [illegible]=[illegible]+F[illegible]
      [illegible]=[illegible]+SINA(K)*SINA(K)
      COSA(K)=[illegible]
      [illegible]=[illegible]+ABS(COSA(K))*COSA(K)*COSA(K)
      FDSIN=F*SINA(K)
      FDCOS=F*COSA(K)
      [illegible]=[illegible]+C[illegible]
      [illegible]=[illegible]+CD
      FNET=((FNET(K)+FNET(K+1))/2.0)-F
      FSIN=FNET*SINA(K)
      FCOSA=FNET*COSA(K)
      CM=CM+FSIN
      CDA=CDA+FCOS[illegible]
      WRITE(6,30)TIME,COSA(K),SINA(K),F,FDCOS,FDSIN,FMMM,FNET,FCOSA,FSIN
      TIME=TIME+DELTAT
  200 CONTINUE
```

```
      [illegible]
      F=(F[illegible]CF(1)+F[illegible]CF([illegible]))/2.0
      COSA(K)=SINA(K+1)
      SINSA=SINS+SINA(K)*SINA(K)
      COSS=COSS+ABS(COSA(K))*COSA(K)*COSA(K)
      FDSIN=F*SINA(K)
      FDCOS=F*COSA(K)
      CD=FDCOS+CD
      CM=FDSIN+CM
      FMMM=((FNET(1)+FNET(NCARD))/2.0)-F
      FSINA=FNET*SINA(K)
      FCOSA=FNET*COSA(K)
      CMW=CMW+FSINA
      CDW=CDW+FCOSA
      FSQAR=F*F
      CAR=CAR+FSQAR
      CA[illegible]=C[illegible]
      CAR=SQRT(CAR/NCARD)
      C2=RHO*((UM*X**2.0)/2.0)*DIA*CL
      CA[illegible]=CA[illegible]/C2
      WRITE(6,30)TIME,COSA(K),SINA(K),F,FDCOS,FDSIN,FMMM,FNET,FCOSA,FSIN
     6A
      STINS=SINS*2.0*PI/NCARD
      CONS=COSS*2.0*PI/NCARD
      Z1=[illegible]/STNS
      Z2=[illegible]/CONS
      CM=Z1*CM*DELTAT
      CD=Z2*CD*DELTAT
      CMW=Z1*CMW*DELTAT

      CDW=Z2*CDW*DELTAT
      CYS=[illegible]
      CFMAX=FMMAX*CM/C2
      CM=CM/CYCLE
      CD=CD/CYCLE
      CMW=CMW/CYCLE
      CDW=CDW/CYCLE
      WRITE(6,11)
      WRITE(6,35)
      WRITE(6,40)CM,CD,BETA,REYNO,CMW,CDW
      WRITE(6,42)
      WRITE(6,43)STRH,CDBAR,VRTOD,FMEAN,FOMEAN,RMS,CFMAX
      WRITE(6,65)
      WRITE(6,75)STNS,CONS,Z1,Z2
      WRITE(6,11)
  100 CONTINUE
   10 FORMAT(3F8.4,I8,F6.4,F10.6,F8.4,I7,I5)
   11 FORMAT('0',3X,75('*'))
   15 FORMAT('1',2X,'DIA=',F8.4,2X,'AMP=',F8.4,2X,'PER=',
     1F6.4,2X,'UMAX=',F6.4,2X,'CM=',F8.4,2X,'NRUN=',I6)
   20 FORMAT('0',2X,'TIME/PER',5X,'COSA',6X,'SINA',9X,'F',5X,'FDCOS',5X,
     1'FDSIN',6X,'FMMM',6X,'FNET',6X,'FNCOS',5X,'FNSIN')
   30 FORMAT('0',10F10.4)
   35 FORMAT('0',7X,'CM=',9X,'CD=',8X,'BETA=',6X,'REYND=',8X,
     1'CMW=',7X,'CDW=')
   40 FORMAT('0',6F12.4)
   42 FORMAT('0',7X,'STRH=',5X,'CDBAR=',7X,'VRTOD=',7X,'FMEAN=',7X,
     1'FOMEAN=',5X,'RMS=',7X,'CFMAX=')
   43 FORMAT('0',7F12.4)
   55 FORMAT(F10.4)
   65 FORMAT('0',7X,'SINS=',5X,'CONS=',8X,'Z1=',9X,'Z2=')
   75 FORMAT('0',4F12.4)
      STOP
      END
```

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