AFAPL-TR-72-28

AN INVESTIGATION OF SCUFFING CONDUCTED ON THE AFAPL DISK TESTER

H. J. Carper E. L. Anderson P. M. Ku

Southwest Research Institute

TECHNICAL REPORT AFAPL-TR-72-28

June 1972

Approved for public release, distribution unlimited.

on Laboratory

Air Force Aero Propulsion Laboratory Air Force Systems Command Wright-Patterson Air Force Base, Ohio

> NATIONAL TECHNICAL INFORMATION SERVICE

NOTICE

When Government crawings, specifications, or other data are used for any purpose other than in connection with a definitely related Government procurement operation, the United States Government thereby incurs no responsibility, nor any obligation whatsoever; and the fact that the Government may have formulated, furnished, or in any way supplied the said drawings, specifications, or other data, is not to be regarded by implication or otherwise as in any manner licensing the holder or any other person or corporation, or conveying any rights or permission to manufacture, use, or sell any patented invention that may in any way be related thereto.



ilan the

Ļ

ACopies of this report should not be returned unless return is required by security considerations, contractual obligations, or notice on a specific document.

L

UNCLASSIFIED			
Security Classification			
DOCUMENT CONT	ROL DATA - R	& D	
(Security classification of title, body of abstract and indexing	annotation must be e	ntered when the	overall report is classified)
1. ORIGINATING ACTIVITY (Corporate author)		28. REPORT SE	CURITY CLASSIFICATION
Southwest Research Institute		Unclassified	d
Son Antonio Touro 70204		2b. GROUP	
San Antonio, rexas 78284		<u> </u>	
3. REPORT TITLE			
AN INVESTIGATION OF SCUFFING CONDUCTED ON THE	E AFAPL DISK T	ESTER	
4. DESCRIPTIVE NOTES (Type of report and inclusive dates)			
Phase Report, April 1, 1971 through January 31, 1972			
5. AUTHOR(5) (First name, middle initial, last name)			
Carper, H. J., Anderson, E.L. and Ku, P. M.			
6. REPORT DATE	70. TOTAL NO. O	F PAGES	7b. NO. OF REFS
June 1972	33 plus 9 Prelin	ninaries	4
88. CONTRACT OR GRANT NO.	98. ORIGINATOR	S REPORT NUME	BER(S)
F33615-69-C-1295			
b. PROJECT NO.	RS-586		
3048			
C. Task No.	95. OTHER REPO	RT NO(S) (Any of	ther numbers that may be assigned
304806	this report)		
d.	AFAPL-TR-	72-28	
10. DISTRIBUTION STATEMENT	. <u></u>		······································
Approved for public release; distribution unlimited	12 SPONSORING		
	Air Force A	ero Propulsion	Laboratory
	Air Force Sy	stems Comman	nd
	Wright-Patte	rson Air Force	Base, Ohio 45433
13. ABSTRACT		<u></u>	
This report presents the results of scuffing studies total of 90 tests were conducted using AMS 6260 CE combinations of sliding and sum velocities, at two lubri disks, and in air and inert environments. The load-ca velocity, increase with increasing sum velocity, decreas belted as against unbelted drive, and decrease in the pre temperature, the minimum lubricant film thickness ratio to the variables investigated are presented and discussed.	conducted on th VM steel disks a cant jet temperatu rrying capacity is e with increasing sence of an inert at failure, and the	e AFAPL disk nd a MIL-L-78 ures, with two seen to decree lubricant jet t environment. T friction coeffic	tester developed earlier. A 808G lubricant at various methods of driving the test ase with increasing sliding temperature, decrease with the variations of the critical cient at failure with respect

DD FORM 1473

U	NCLASSIFIED	
Securi	ty Classification	

14. KEY WORDS	LINK A		LINK B		LINK C	
	ROLE	WΤ	ROLE	WT	ROLE	wт
Load-Carrying Capacity, Gear						
Scuffing, Gear						
16st, Disk Tastar, AFADI Disk						
Icstel, AFAL DISK						
				Į		
	:					
				1		
· ·						
		1				
			F			
		1				
	ļ					
	1					
		ĺ				
				CIPIPP		
10 –		Security	Classifi	SIFIED		

AN INVESTIGATION OF SCUFFING CONDUCTED ON THE AFAPL DISK TESTER

A Carlo State State State State State

SALANDAR PUBLICATION PORTAL STREET, SALANDAR ST

The second se

H. J. Carper E. L. Anderson P. M. Ku

Approved for public release; distribution unlimited.

İb

FOREWORD

This report was prepared by Southwest Research Institute, 8500 Culebra Road, San Antonio, Texas, under Contract F33615-69-C-1295. The contract was initiated under Project No. 3048, "Fuels, Lubrication, and Hazards," Task No. 304806, "Aerospace Lubrication." The work was administered by the Lubrication Branch, Air Force Aero Propulsion Laboratory, Air Force Systems Command, Wright-Patterson Air Force Base, Ohio. The project engineers were Messrs. E.A. Lake and H.A. Smith (AFAPL/SFL).

The report covers one phase of work performed under the subject contract in the period of April 1, 1971 through January 31, 1972. This report was submitted by the authors in March 1972.

The contractor's report number is RS-586.

Publication of this report does not constitute Air Force approval of the report's findings or conclusions. It is published only for the exchange and stimulation of ideas.

K. J. Birkey

K.L. Berkey Chief, Lubrication Branch Fuels and Lubrication Division Air Force Aero Propulsion Laboratory

ABSTRACT

This report presents the results of scuffing studies conducted on the AFAPL disk tester developed earlier. A total of 90 tests were conducted using AMS 6260 CEVM steel disks and a MIL-L-7808G lubricant at various combinations of sliding and sum velocities, at two lubricant jet temperatures, with two methods of driving the test disks, and in air and inert environments. The load-carrying capacity is seen to decrease with increasing sliding velocity, increase with increasing sum velocity, decrease with increasing lubricant jet temperature, decrease with belted as against unbelted drive, and decrease in the presence of an inert environment. The variations of the critical temperature, the minimum lubricant film thickness ratio at failure, and the friction coefficient at failure with respect to the variables investigated are presented and discussed.

TABLE OF CONTENTS

••

									Paga
Ι.	INTF	RODUCTION							1
	1. 2.	General	 	 	 	•••	•	· •	1 1
11.	TESI	T APPARATUS					•		2
	1. 2.	General	 	 	 	 	•	 	2 2
11 .	IMPF	ROVED CALIBRATION RESULTS							3
	1. 2. 3.	Improved Machine Loss Calibration	 sks	 	 	 		 	3 5 7
IV.	RESU								10
	1. 2. 3. 4. 5. 6. 7. 8.	General	· · · · · · · · · · ·	· · · · · · · · · ·	· · · · · · · · · · · · · · · · · · ·	· · · · · · · · ·	•	· · · · · · · · · · ·	10 11 12 13 14 18 19 20
V.	CON	ICLUSIONS							22
АРР	ENDI	X I-DETERMINATION OF CONSTANTS IN EQ. (6)			•				23
APP	ENDI	X II-SUMMARIES OF TEST RESULTS			•	• •			25
REF	EREN	NCES							33

v

N.COM

LIST OF ILLUSTRATIONS

والانتخاب والمعرود

and the second states of

Figure		Page
1	The Belted Drive System	2
2	Comparison of Machine Loss Calibration Methods Using Lubricant O-67-22	3
3	Thermocouple Hole Locations in Test Disk	5
4	Typical Temperature Distributions in Test Disks	6
5	Comparison of Surface Temperatures of Test Disks at $T_j = 140^{\circ}$ F	6
6	Relationship Between Disk Surface Temperature and Upper and Lower Thermocouple Probe Temperatures for $T_j = 140^{\circ}$ F	7
7	Comparison of Surface Temperatures of Test Disks at $T_j = 190^{\circ}$ F	7
8	Relationship Between Disk Surface Temperature and Upper and Lower Thermocouple Probe Temperatures for $T_f = 190^{\circ}$ F	8
9	Temperature Calibration for Lubricant O-67-22	8
10	Comparison Between Temperature Calibrations Using Lubricants BC and O-67-22 at $T_j = 140^{\circ}$ F	9
11	Effect of Sliding Velocity on Scuffing Failure Load at Constant Sum Velocity $V_t = 1050$ ips .	12
12	Logarithmic Presentation of Data from Figure 11	13
13	Effect of Sliding Velocity on Critical Temperature at Constant Sum Velocity $V_t = 1050$ ips	13
14	Effect of Sliding Velocity on Minimum Film Thickness Ratio at Failure at Constant Sum Velocity $V_t = 1050$ ips	14
15	Effect of Sliding Velocity on Friction Coefficient at Failure at Constant Sum Velocity $V_t = 1050$ ips	14
16	Effect of Sum Velocity on Scuffing Failure Load at Constant Sliding Velocity $V_s = 350$ ips	14
17	Logarithmic Presentation of Data from Figure 16	15
18	Effect of Sum Velocity on Critical Temperature at Constant Sliding Velocity $V_s = 350$ ips	15
19	Effect of Sum Velocity on Minimum Film Thickness Ratio at Failure at Constant Sliding Velocity $V_s = 350$ ips	16
20	Effect of Sum Velocity on Friction Coefficient at Failure at Constant Sliding Velocity $V_s = 350$ ips	16
21	Effect of Velocity Ratio on Scuffing Failure Load	17
22	Effect of Velocity Ratio on Critical Temperature	17

LIST OF ILLUSTRATIONS (Cont'd)

Figure		Page
23	Effect of Velocity Ratio on Minimum Film Thickness Ratio at Failure	17
24	Effect of Velocity Ratio on Friction Coefficient at Failure	18
25	Effect of Velocity on Scuffing Performance Parameters at Constant Velocity Ratio	18
26	Weibull Plot of Scuffing Failure Load at $V_t = 1050$ ips	20
27	Weibull Plot of Scuffing Failure Load at $V_s = 350$ ips	21

LIST OF TABLES

Ser.

Table		Page
I	Summary of Test Series and Conditions with Batch B AMS 6260 Disks and Lubricant O-67-22 .	10
II	Comparison of Average Test Results for Batch A and B Test Disks with Lubricant O-67-22, $T_j = 140^{\circ}$ F, $V_s = 630$ ips, $V_i = 1050$ ips	11
111	Comparison of Average Test Results for $V_s = 630$ ips and $V_t = 1050$ ips Using Lubricant O-67-22 at Two Lubricant Jet Temperatures	19
IV	Comparison of Average Test Results for $V_s = 450$ ips, $V_t = 1050$ ips, and $T_j = 190^{\circ}$ F. Using Lubricant O-67-22 in Air and Inert Environments	19
v	Summary of Results for Series VIII	27
VI	Summary of Results for Series IX	27
VII	Summary of Results for Series X	28
VIII	Summary of Results for Series XI	28
IX	Summary of Results for Series XII	29
х	Summary of Results for Series XIII	29
XI	Summary of Results for Series XIV	30
XII	Summary of Results for Series XV	30
хш	Summary of Results for Series XVI	31
χιν	Summary of Results for Series XVII	31
ху	Summary of Results for Series XVIII	32

LIST OF SYMBOLS

a	major axis semiwilth of conjunction ellipse, in.
ħ	minor axis semiwidth of conjunction ellipse, in.
E	Young's modulus, psi (E_1 for upper disk, E_2 for lower disk)
* E	equivalent Young's modulus, psi
	$= 2 \left[(1 - v_1^2) / E_1 + (1 - v_2^2) / E_2 \right]^{-1}$
ſ	disk friction coefficient = T_f/rP
f_f	disk friction coefficient at scuffing failure
h _m	minimum elastohydrodynamic film thickness, µin.
h _{mf}	minimum elastohydrodynamic film thickness at scuffing failure, μ in.
М	ratio of sliding velocity to sum velocity = V_s/V_r
mm	metal monitor reading
N	rotational speed of disks, rpm (N_1 for upper disk, N_2 for lower disk)
Р	disk load, lb
P _f	disk load at scuffing failure, lb
r	disk radius, in. r_1 for upper disk, r_2 for lower disk)
R	equivalent we have of disk pair, in. = $r_1 r_2 / (r_1 + r_2)$
S	ratio of disx rotational speeds = N_1/N_2
ΔT	conjunction temperature rise, °F
T _{cr}	itical temperature, °F
T_{f}	disk friction torque, inlb
T _j	lubricant jet temperature, °F
T_m	machine loss torque, inlb $(T_{m1}$ for upper shaft, T_{m2} for lower shaft)
T _p	thermocouple probe temperature, °F (T_{p1} for probe riding on upper disk, T_{p2} for probe riding on lower disk)
Σ,	reaction torque, inlb (T_{r1} for upper shaft, T_{r2} for lower shaft)
7's	disk surface temperature, °F (T_{s1} for upper disk, T_{s2} for lower disk)

LIST OF SYMBOLS (Cont'd)

ĵ,

and a subject of the solution of the second

1

ades frontenes (alberta bloud

V	peripheral velocity of disk surface, ips (V_1 for upper disk, V_2 for lower disk)
V _s	sliding velocity, ips = $V_1 - V_2$
V _t	sum velocity, ips = $V_1 + V_2$
w _e	equivalent load, ppi
αο	pressure-viscosity coefficient of lubricant at conjunction inlet temperature and atmospheric pressure, psi ⁻¹
β	Blok's thermal coefficient, lb/°F-insec ^{1/2}
δ	composite surface roughness of a pair of disks, μ in. CL $\alpha = \delta_1 + \delta_2$ (δ_t for initial, δ_a for after break-in, δ_f for after failure)
δι	composite surface roughness of upper disk, μ in. CLA = $(\delta_{x1} + \delta_{y1})/2$
δ_2	composite surface roughness of lower disk, μ :n. CLA = $(\delta_{x2} + \delta_{y2})/2$
δ_x	surface roughness of disk measured in direction of motion, μ in. CLA
δ _y	surface roughness of disk measured perpendicular to direction of motion, μ in. CLA
Λ_m	minimum lubricant film thickness ratio = h_m/δ_a
Λ_{mf}	minimum lubricant film thickness ratio at failure = h_{mf}/δ_a
μ_o	lubricant viscosity at conjunction inlet temperature and atmospheric pressure, cp
μ _j	lubricant viscosity at lubricant jet temperature and atmospheric pressure, cp
ν	Poisson's ratio of disk material (ν_1 for upper disk, ν_2 for lower disk)
σ	stress, psi ($\overline{\sigma}$ for mean stress, σ_m for maximum stress)

SECTION I

INTRODUCTION

1. General

In a previous report $(1)^*$ the authors described in detail a disk tester for gear lubrication research, designed and developed by Southwest Research Institute (SwRI) for the Air Force Aero Propulsion Laboratory (AFAPL). Also included in that report were the results of some preliminary scuffing tests conducted using the new tester. These tests were considered preliminary because operating procedures were being developed for the tester as data were being collected, and the machine operator was gaining experience in test control and scuffing failure detection. In addition, the torquemeters used in the earlier tests had a capacity that was too large compared with the reaction torques that were being observed on the disk shafts. These torquemeters have since been replaced with ones of smaller capacity. As will be seen later, the new triquemeters have considerably improved the accuracy in the determination of the disk friction.

This report presents the results of a program conducted using the AFAPL disk tester to determine the effects of sliding velocity, sum velocity, lubricant jet temperature, and environmental atmosphere on the scuffing phe nomena of disks made from a typical gear steel (AMS 6260) and lubricated with a common synthetic gas turbine engine lubricant (MIL-L-7808G). In addition, the results of new machine loss and temperature calibrations used in the calculation of the test results are also presented.

2. Experimental and Analytical Approach

The philosophy of SwRI's approach to the scuffing problem has been dealt with previously.⁽¹⁾ Briefly, it is felt that even though the load-carrying capacity is ultimately the quantity of most practical interest to designers, it is not a fundamental quantity that governs the scuffing process. This is evident from the lack of direct correlation of the load-carrying capacity data from the various test devices and test methods. To find a more rational means of lubricant selection and gear performance prediction, scuffing studies at SwRI have concentrated on the evaluation of more fundamental approaches, such as the critical temperature hypothesis,^(2,3) the elastohydrodynamic (EHD) film thickness theory,⁽⁴⁾ and other possible criteria.

With appropriate instrumentation on the disk tester, the quantities necessary for input to the expressions for critical temperature and minimum LHD film thickness are measured and these two quantities calculated. Then the behavior of these quantities with such operating conditions as speed, lubricant temperature, and environmental atmosphere, etc., can be examined and evaluated.

The relationships used for the calculation of critical temperature and EHD film thickness were presented in detail in the earlier report.(1)

^{*}Superscript numbers in parentheses refer to the list of References at the end of this report,

SECTION II

TEST APPARATUS

1. General

A complete description of the AFAPL disk tester, together with test disk geometry, the instrumentation, the calibration and operating procedures, was presented in Reference 1.

Two major changes in the test equipment were introduced during this reporting period. One was the replacement of the two 2000-in.-lb torquemeters with two 500-in.-lb torquemeters. The 500-in.-lb torquemeter capacity was selected so as to improve the accuracy in the measurement of the reaction torques on the two shafts, from which the disk friction torque must be derived. The reaction torques encountered in the program ranged from approximately 25 to 250 in.-lb. The 500-in.-lb capacity was the smallest capacity available that would satisfy the other application requirements.

The other change was the provision for an alternative drive system, which is described in the following section.

2. Unbelted and Belted Drive Systems

As originally designed, the upper disk of the AFAPL disk tester is driven by a motor, and the lower disk is connected to a moto./brake unit that can hold the lower disk at any speed equal to or different from that of the upper disk. With independent, stepless speed controls on the motor and motor/brake units, any combination of sliding and sum velocities can readily be obtained. This is a highly desirable feature for a research apparatus intended to cover a wide range of sliding to sum velocity ratios.



FIGURE 1. THE BELTED DRIVE SYSTEM

A more conventional type of drive system is one in which the two disk shafts are connected by either phase gears, or belts and pulleys. With this arrangement, the sliding to sum velocity ratio is fixed. Thus, each time a different sliding to sum velocity ratio is desired, the phase gear ratio or pulley diameter ratio must be changed. This type of drive system is less versatile; however, it is the type more commonly used in gear hubrication and scuffing research.

In order to determine whether there would be a difference in results if the disk tester were operated in the conventional

as compared with the intended manner, a pulley and timing belt as angement was devised to 'gear" the two shafts together. Figure 1 is a photograph of this belted drive system. Theorethe belting arrangement shown, a motor drives the lower shaft which, in turn, drives a direction-reversing gearbox wide a 2.1 gear ratio. The pulley on the output side of the gearbox then drives the upper shaft. By changing the pulley sills – various combinations of shding and sum velocities can be obtained. The quite interesting difference in results obtained by operating the tester in these two ways is discussed later.

SECTION III

IMPROVED CALIBRATION RESULTS

1. Improved Machine Loss Calibration

After the new torquemeters were installed, a second machine loss calibration was conducted using lubricant O-67-22 (MIL-L-7808G) and the procedure previously outlined.⁽¹⁾ The results were in general agreement with the previous ones obtained using the less sensitive torquemeters in that the relationship between machine-loss torque and disk load was linear at constant shaft speed and lubricant viscosity. Also, a linear relationship between machine-loss torque and shaft speed at constant load was observed.

The machine loss was derived by two techniques which, for convenience, will be referred to hereafter as the old and new techniques.

Old Technique. It was previously shown⁽¹⁾ that for the upper disk shaft

$$T_{r1} = T_f + T_{m1}$$
(1)

and for the lower disk shaft

$$T_{r2} = T_f - T_{m2} \tag{2}$$

where

 T_f – disk friction torque, in.-lb

- T_{r1} reaction torque indicated by torquemeter on the upper shaft, in.-lb
- T_{r2} reaction torque indicated by torquemeter on the lower shaft, in.-lb

 $T_{m,1}$ – machine-loss torque for upper shaft, in.-lb

 T_{m2} - machine-loss torque for lower shaft, in.-lb

In Eqs. (1) and (2), T_f is obviously the same under any condition of operation. Further, if the calibration is performed with the two shafts operating at precisely or approximately the same speed, then for identical bearings and shafts

$$T_m = T_{m1} = T_{m2} \tag{3}$$

where T_m is the machine-loss to que, in.-lb, at the specific speed, load, and other operating conditions at which the calibration is performed. With this assumption, subtracting Eq. (2) from Eq. (1) yields

$$T_m = \frac{T_{r1} - T_{r2}}{2} \tag{4}$$

Such a calibration was performed with lubricant O-67-22 and the new torquemeters. The results are shown as dashed

curves in Figure 2, in which the data points have been omitted for the sake of clarity. As mentioned previously, these results are in general agreement with those reported in Reference 1.



LUBRICANT 0-67-22



New Technique. When the data reduction on the scuffing test results began, differences were noted in the two values of the disk friction coefficient at failure derived from the two torquemeter readings. A difference was also observed in the preliminary scuffing studies, but this was attributed to the fact that the first machine loss calibration contained some inaccuracies due to the less sensitive torquemeters.⁽¹⁾

In order to bring the two friction coefficients into better agreement, a different procedure was tried. This procedure involved first assuming a functional relationship for the machine-loss torque in terms of speed, load, lubricant viscosity, and undetermined constants. Then, using the fact that the coefficients of friction as derived from the two torquemeters must, by definition, be equal, equations were developed that allowed the constants in the machine loss equation to be calculated from experimental data.

With the new technique, it is no longer necessary to operate the two shafts at approximately the same speed. Rather, the shafts may operate at any speeds, as in the actual scuffing tests. Thus, the method has the advantage that the torquemeter readings obtained during scuffing tests can be used to determine the constants in the machine loss equation. These larger readings are more accurate than the relatively small readings obtained in determining the machine-loss torques by the old technique.

As can be seen in Figure 2, by the old technique it was found that the machine-loss torque varied linearly with load at constant speed and lubricant viscosity. It can also be seen that, at constant load and lubricant viscosity, the variation of machine-loss torque with speed is approximately linear. Based on these two observations, and being guided by the results from the old technique with respect to the effect of viscosity, a general expression for the machine-loss torque may be written as

$$T_m = aP + bN\mu_j^c \tag{5}$$

where

 T_m – machine-loss torque for one shaft, in.-lb

P – disk load, lb

N – shaft speed, rpm

 μ_i – lubricant viscosity at T_i and atmospheric pressure, cp

a,*b*,*c* -- fitting constants

Since the two shafts are identical and are mounted in identical bearings, it is assumed that Eq. (5) can be applied to either shaft under any set of operating conditions. Thus the results obtained in the actual scuffing tests can be employed directly to determine the fitting constants a, b, and c. The details of the procedure are given in Appendix I; the resulting equation for the machine-loss torque is

$$T_m = 0.0023P + 0.000968N\mu_i^{0.23} \tag{6}$$

where

 T_m machine-loss torque, in.-lb (T_{m1} for upper shaft, T_{m2} for lower shaft)

P - disk load, lb

- N disk speed, rpm (either N_1 or N_2 depending on whether the upper or the lower disk shaft is under consideration)
- μ_i lubricant viscosity at T_i and atmospheric pressure, cp

The calculated results from Eq. (6) are shown as solid curves in Figure 2, for compariso with the results derived from the old technique shown as dashed curves. Note that the new technique predicts h her machine-loss torques than the old technique, particularly at high disk loads.

The discrepancy of results from the old and new techniques underscores the basic difficulty with all types of disk testers for which the disk friction must be derived from the reaction torques measured on the shafts which, by necessity, include the contributions of the support bearings. It is a moot question which technique gives a better prediction of the machine-loss torque. The crucial question is which technique gives a better correlation of the disk friction coefficient. Obviously, since the disk friction coefficient must, by definition, be the same for the upper disk as for the lower disk, then the new technique, which recognizes this fact, is to be preferred.

Of course, in the derivation of the "smoothed" data presented in Figure 2, experimental errors in obtaining the raw data and the subsequent treatment of the raw data all tend to introduce some uncertainties. In the last analysis, it does not matter which technique gives the "correct" machine-loss torque, but rather which technique gives the "correct" disk friction coefficient which nature demands to be equal on the basis of either the upper or lower disk. From extensive analysis and comparison of all of the scuffing test results available from the AFAPL disk tester over the entire range of variable, employed to date, it has been found that the two disk friction coefficients at failure derived from machine-loss data obtained by the old technique differ in most cases by approximately 10 percent and occasionally by as much as 20 percent. On the other hand, by using machine-loss data obtained by the new technique, the two friction coefficients usually agree to within 5 percent and only in rare instances to about 10 percent. On the basis of these findings, it is concluded that the new technique gives more plausible .esults.

2. Simultaneous Temperature Measurements in Upper and Lower Disks

As explained previously,⁽¹⁾ the AFAPL disk tester is equipped with a thermocouple probe located in the center plane of the disks 1/3 in, ahead of the center of the conjunction. This thermocouple probe may be made to suspend freely without touching either disk or to ride with a slight pressure on one of the disks. Due to the complex oil flow and heat transfer conditions prevalent at this vicinity, the temperature across the oil film is generally not uniform, and the probe reading with the probe touching the upper (or faster) disk is always higher than the reading with the probe touching the lower disk sometimes by as much as 30° F. In any case, regardless of the probe's position, it does not read directly the surface temperature of either disk.

In order to derive the surface temperature, T_s , of a disk from the probe temperature, T_p , with the probe touching that disk, the relationship between the disk surface temperature and the corresponding probe temperature must be known. Such a relationship can only be established by calibration,⁽¹⁾ which involves instrumenting a disk with embedded thermocouples and taking simultaneous readings from the embedded thermocouples and the thermo-couple probe riding on the same disk. As mentioned previously, however, the thermocouple probe always reads higher when riding on the upper disk than when riding on the lower disk. The question then arises as to whether the T_s vs T_p relationship is or is not the same for the upper and lower disks. Indeed, if this relationship were the same for both disks, then the surface temperature of the upper (or faster) disk must necessarily be higher than that of the



IN TEST DISK

lower disk. This would then yield two calculated critical temperatures, which would be difficult to interpret.

In order to determine if the two surface temperatures were different, simultaneous temperature measurements were made on both the upper and lower disks. For this work, the holes drilled in the calibration disks were relocated as shown in Figure 3. As can be seen by comparing this figure with Figure 14 in Reference 1, a hole was placed between the first and second original holes to allow a more accurate determination of the gradient near the surface, hence a more accurate extrapolation to the surface temperature.

Both the upper and lower disks were instrumented with embeddea thermocouples, and two identical mercury slip rings were installed on the two shafts to connect the embedded thermocouples to the temperature recorder. The disk tester was then operated with lubricant O-67-22 at various conditions of speed and load, and the temperatures at three different radial positions (as limited by the capacity of the mercury slip rings) in the upper and lower disks were recorded simultaneously. Also, at each condition, the thermocouple probe readings with the probe riding on the upper and lower disks were both obtained. This procedure was carried out at $T_i = 140^\circ$ and $190^\circ F$, but more extensively at $T_i = 190^{\circ}$ F since the majority of the scuffing tests were to be conducted at the higher lubricant jet temperature.

Figure 4 shows typical temparature distributions in the disks for one set of speed conditions, where $V_s = 300$ ips and $V_t = 200$ ips. Figure 4a was obtained with $T_j = 140^{\circ}$ F, while Figure 4b was obtained with $T_j = 190^{\circ}$ F. The three curves were generated by using three different values of disk load. The surface temperature was obtained thermocouple tempera-

tures, assuming a linear relationship between the disk temperature and the logarithm of the radial distance. This procedure has been used as a standard technique throughout all the calibration work. As shown in the figure, at each radial position there is little difference in the embedded thermocouple temperatures, which results in the extrapolated surface temperatures being nearly the same for both the upper and lower disks.

Figure 5 shows all the results for $T_j = 140^{\circ}$ F, where the surface temperature for the upper disk is plotted against the surface temperature for the lower disk. As shown in the figure, data were obtained for three values of sliding to sum velocity ratio, M. The dashed line shown in the figure is where $T_{s_1} = T_{s_2}$, and, as can be seen, the surface temperatures agree well with this relationship. It will be noted that the disk surface temperatures do not extend quite as high as some of the surface temperatures obtained in the scuffing tests. This is because the maximum load was interationally kept low in order not to scuff the calibration disks, which are extremely time-consuming to make and were limited in quantity. In about half of the scuffing tests, at







 $T_1 = 140^{\circ}$ F, the surface temperature was approximately 200°F or lower, which was the highest temperature observed in the simultaneous temperature measurements.

Figure 6 shows the relationship between the extrapolated disk surface temperature and the corresponding thermocouple probe temperature for the same conditions presented in Figure 5. It is apparent that, although the surface temperatures are nearly equal as shown in Figure 5, the thermocouple probe temperatures are not the same



FIGURE 6. RELATIONSHIP BETWEEN DISK SURFACE TEMPERATURE AND UPPER AND LOWER THERMOCOUPLE PROBE TEMPERATURES FOR $T_i = 140^{\circ}$ F





for the two disks. For a given surface temperature, the thermocouple probe temperature on the upper disk is always higher than that on the lower disk.

Figure 7 shows the simultaneous surface temperature determinations for $T_j = 190^{\circ}$ F. The data were obtained for the embedded thermocouple temperatures at five values of M. The two highest ratios were identical to those used in many of the scuffing tests. As can be seen, the two surface temperatures are r in good agreement over the whole range. The highest extrapolated surface temperature obtained was about 265°F, which is as high as the surface temperature at scuffing for the majority of the tests conducted at $T_i = 190^{\circ}$ F.

The relationship between the extrapolated disk surface temperatures presented in Figure 7 and the corresponding thermocouple probe temperatures is shown in Figure 8. Similar to the case where $T_j = 140^{\circ}$ F, the thermocouple probe temperatures are not the same for the two disks. Likewise, for a given surface temperature, the probe temperature is always higher for the upper disk than for the lower disk.

It is clear from the above that with lubricated disks operated over a wide range of conditions, both the temperature distribution inside the two disks and the surface temperatures of the two disks are the same. However, the thermocouple probe temperatures with the probe touching the upper and lower disks are generally different. Therefore, the T_s vs T_p relationships for the two disks are different. In practice, either the upper or the lower disk may be employed to deduce disk surface temperature, provided the appropriate T_s vs T_p calibration is used.

3. Improved Temperature Calibration

Having ascertained that either the upper or the lower disk may be used to derive the disk surface temperature, a more detailed calibration was made with lubricant O-67-22 over a wide range of conditions. This calibration was made using the new calibration disks shown in Figure 3, and by careful placement of the thermocouple probe at a distance of 1/3 in, ahead of the center of the disk conjunction. Following the earlier practice⁽¹⁾, the lower disk was used as the basis for the temperature calibration.

Figure 9 shows the relationship between the extrapolated surface temperature, T_s , and the thermocouple probe temperature $T_{p,2}$, for the two lubricant jet temperatures of 140° and 190°F. In contrast with the results previously



obtained using lubricant BC⁽¹⁾, no speed effect was noted with lubricant O-67-22. That is, the T_s vs T_p relationship is a single curve for all disk speeds between 955 and 3342 rpm, and is linear. The slope of this curve is dependent upon the lubricant jet temperature, T_j . For $T_j = 140^{\circ}$ F, the extrapolated surface temperature is given by

$$T_s = 140 + 4.286 \left(T_{p2} - 140 \right) \tag{7}$$

and for $T_i = 190^{\circ}$ F

$$T_s = 190 + 10.000 \left(T_{p2} - 190 \right) \tag{8}$$

where

 T_s – disk surface temperature, °F

T_{p2} - thermocouple probe temperature, °F (with probe riding on the lower disk)

It will be noted that the extrapolated surface temperature is extremely sensitive to changes in the thermocouple probe temperature. Thus, slight differences in the thermocouple probe temperature caused by factors such as probe installation or characteristics of the probe itself can introduce large errors in the predicted disk surface temperature, which is used to calculate critical temperature. For this reason, extreme precautions must be taken in the construction and installation of the thermocouple probes in order to ensure consistent conditions from test to test. Figure 10 compares the single curve obtained with lubricant O-67-22 at $T_j = 140^{\circ}$ F, with the curves obtained using lubricant BC at the same temperature. As can be seen, there is considerable difference in the results for the two lubricants. This demonstrates the need for the temperature calibration to be conducted using the same lubricant that will be used in the actual scuffing tests. It is believed that the difference between the results can be explained by the fact that lubricant BC is about seven times as viscous as lubricant O-67-22 and thus results in a thicker lubricant film that affects the thermocouple probe temperature.



FIGURE 10. COMPARISON BETWEEN TEMPERATURE CALIBRATIONS USING LUBRICANTS BC AND 0-67-22 AT $T_j = 140^{\circ}$ F

SECTION IV

RESULTS AND DISCUSSION

1. General

This section presents the experimental results of 90 scuffing tests conducted using disks operating at various combinations of sliding and sum velocities, with lubricant O-67-22 at two lubricant jet temperatures, and in air and nitrogen environments.

The test disks used in these experiments were fabricated from a single heat of AMS 6260 CEVM steel and represent the second batch of disks, Batch B, tested on the AFAPL disk tester. The first batch of disks, Batch A, also of AMS 6260 steel, were all tested at a lubricant jet temperature $T_j = 140^{\circ}$ F, but using three different lubricants. The results of these tests have been reported previously.⁽¹⁾

The properties of lubricant O-67-22, a MIL-L-7808G lubricant, have been previously given in Reference 1.

The majority of the Batch B disks were tested at $T_j = 190^{\circ}$ F. However, some tests were conducted at $T_j = 140^{\circ}$ F for comparison with the results obtained with Batch A.

In the experience of SwRI with a very large number of disk tests, it has been noted that even with test disks made under very stringent control from a single heat of steel, and with tests conducted under the utmost care, replicate tests, as a rule, yield scuffing failure loads of considerable scatter, typically by a factor of 2 to 3. In the interest of statistical confidence, it was decided that, in testing with Batch B disks, 10 replicate tests should be conducted at each set of test conditions so that reliable averages would be obtained. This was done for 7 series of tests, thus accounting for 70 of the 90 tests conducted. It was then discovered that quite different test results were obtained by driving the test disks with the two alternate drive systems mentioned earlier. To explore the drive-system effect c or the widest range of operating conditions possible with the remaining Batch B disks, it was necessary to run only 5 replicate tests for each of the 4 remaining series.

Before discussing the results, it will be helpful to summarize the test program by tabulating the test series numbers and the test conditions for each series. This is done in Table I. Seven test series, Series VIII to XIV, were

conducted with the unbelted drive system. Series X, XI, and IX were intended to determine the effect of changing sliding velocity at constant sum velocity, while Series X, XII, and XIII were conducted to determine the effect of changing sum velocity at constant sliding velocity. Note that Series X is common to both groups of three series. Series VIII was conducted at $T_j = 140^{\circ}$ F, both for comparison with Batch A results and for comparison with Series !X to determine the effect of lubricant jet temperature, T_j . Series XIV was to be compared with Series XI for the effect of an inert environment.

In the belted mode of operation, comparison of Series XV and XVI would show the effect of varying sliding velocity while holding sum velocity constant, while comparison of Series XV and XVII would show the effect of varying sum velocity while holding sliding velocity constant. Here Series XV is common to

TABLE I. SUMMARY OF TEST SERIES AND CON-DITIONS WITH BATCH B AMS 6260 DISKS AND LUBRICANT 0-67-22

Series	No. of tests	V ₅ , 195	17. 195	$M = V_{\rm S}/V_{\rm I}$	s	<i>Т</i> ј. °1	Invironment						
Unbelted drive													
X	10	350	1050	0.333	2.0	190	Ан						
XI	19	450	1050	0.429	2.5	190	Ан						
IX	10	630	1050	0.600	4.0	190	Ан						
X	10	350	1050	0.333	2.0	190	Air						
XII	10	350	817	0.429	2.5	190	Air						
XIII	10	350	583	0.600	4.0	190	Air						
VIII	10	630	1050	0.600	4.0	140	Au						
XIV	10	450	1050	0.429	2.5	190	Nitrogen						
			l	Belted drive									
XV	5	350	1050	0.333	2.0	190	Air						
XVI	5	450	1050	0.429	2.5	190	Air						
XV	5	350	1050	0.333	2.0	190	Air						
XVII	5	350	817	0.429	2.5	190	Air						
xvm	5	200	600	0 333	2.0	190	Ан						

both groups of series. Series XVIII was conducted to obtain results at M = 0.333 by using a different combination of V_s and V_t to compare with Series XV.

The detailed results of the various test series are presented in Appendix II. Each table in Appendix II contains the specific break-in and test conditions of each series, the results of each individual test in the series, as well as the average results of the series.

In the following material, the various effects will be examined on the basis of the average results of the appropriate test series. In the examination of the various effects, all parameters of performance such as the failure load, the friction coefficient at failure, the critical temperature, and the minimum lubricant film thickness ratio at failure will be compared. As mentioned previously, from the viewpoint of evaluating the validity of some form of a generalized scuffing criterion, such parameters as the critical temperature, the film thickness ratio, or some other suitable index should be of more fundamental interest. However, the calculation of all these generalized indices involves some assumptions or arbitrary decisions. Currently, the amount of research does not permit an evaluation of the generalized indices to be made with configure. Therefore, pending additional research, greater emphasis will be given to an examination of the failure load and the friction coefficient at failure. Both parameters are derived from actually measured quantities and are not subject to arbitrary situations other than measurement errors.

2. Comparison of Results for Batch A and B Test Disks

Before beginning the test program with Batch B disks, it was desirable to conduct at least one series of tests under the same conditions used for Batch A disks. This comparison was made for several reasons. (1) the disks, even though made of the same material, were manufactured by two different companies from different heats of steel, (2) Batch B disks were, in general, slightly rougher than Batch A, and (3) the new, more sensitive torquemeters permitted a check on the friction coefficient results previously obtained in testing Batch A disks.

Ten tests were conducted using Batch B disks at $T_1 = 140^{\circ}$ F, $V_s = 630$ ips, and $V_1 = 1050$ ips. For these tests, the disk tester was operated in the unbelted mode (two shafts independently driven), as was done in all tests with

Before break-in			Aten	d of br	eak∙in	At failure									
Batch	δ1/δ2, µin.	δį, µin.	δ ₄ , µin.	h _m , μin.	۸m	δſ. µin.	h _{mf} . μin.	∧ _{mf} •	ff	P _f . 1b	w _e , ppi	Tp2. F	r₅. °F	Δ <i>Τ</i> . °F	°F
A B	8.6/9.3 12.0/13.2	17.9 25.2	14.0 15.1	3.5 4.6	0.26 0.31	19.4 22.3	12.3 13.3	0.88 0.88	0.014 0.022	762 655	6179 5535	169 162	263 231	146 227	409 458
*Based	on s _a .														

TABLE II. COMPARISON OF AVERAGE TEST RESULTS FOR BATCH A AND B TEST DISKS WITH LUBRICANT 0-67-22, $T_i = 140^\circ$ F, $V_s = 630$ ips, $V_l = 1050$ ips

Batch A disks. Table II compares the results with those obtained using Batch A. This table shows the average values of the measured and calculated quantities for 5 tests on the Batch A disks and the 10 tests on Batch B disks. The Batch A results are taken from Table IX of Reference 1. However, they have been modified to correct for the new machine loss and temperature calibrations obtained for lubricant O-67-22. The previously reported values were obtained using the calibration results for lubricant BC, which were the only values available when Reference 1 was published.

As can be seen by comparing these corrected results for Batch A disks with those in Table IX of Reference 1, the main effect of using the new calibration data is to increase the disk surface temperature, T_s . Other quantities remained relatively unaffected by the new calibration results.

In comparing the results of the two batches of disks in Table II, note that the average initial composite surface roughness, δ_1 , of Batch B disks is somewhat larger than that of Batch A. However, the break in process brings the

two measured roughnesses 'o within 1 µin. of each other. At failure, the calculated minimum film thickness differs by only 1 µin. The film thickness ratio at failure, Λ_{mf} , is identical for the two batches. It should be noted that the film thickness ratio at failure should ideally be based on the calculated minimum film thickness and the composite surface roughness at, or immediately prior to, failure. In practice, however, the surface roughness of the disks cannot be measured at, or immediately prior to, failure. Thus the δ_f measurements must be made after failure. In most cases, the scuffing had progressed all around the disks by the time the machine was stopped, and considerable further damage to the surfaces had occurred. Consequently, no reliable measurement of δ_f can be made. For this reason, this report has based Λ_{mf} on δ_a , the composite surface roughness after break-in, rather than on δ_f .

The friction coefficient is considerably lower for Batch A disks. This difference is attributed mainly to the inaccuracy of the torquemeters that were used in tests with Batch A disks rather than to the difference in surface roughness. From extensive experience with disk testing at SwRI, such a large variation in the friction coefficient at failure with surface roughness has never been observed.

The failure load, P_f , for Batch B disks is 14 percent lower than that for Batch A, and may be attributed to the combination of the effects of a different material batch and disk surface roughness. This difference in P_f is real because P_f is a directly measured quantity. The thermocouple probe temperature, T_{p2} , and the disk surface temperature, T_s , at failure are both higher for Batch A disks due to the Ligher P_f . For otherwise constant conditions, these two temperatures should be some function of $f_f P_f$. From this consideration, it is seen that the f_f for Batch A disks must be too low. This error in f_f causes the ΔT at failure to be too low for Batch A disks; consequently, the critical temperature, T_{cr} , for Batch A disks must be too low.

3. Effect of Sliding Velocity

The effect of sliding velocity on the failure load is shown in Figure 11. Roman numerals denoting the test series are placed near the data points to and in identifying the corresponding test conditions shown in Table 1. The upper curve in the figure was obtained while operating the tester with the unbelted drive, the lower curve was obtained while operating the tester with the belted drive. For both operating modes, the failure load decreases with increasing V_s . At low values of sliding velocity, the failure load is 2 to 3 times higher when the drive shafts are not belted together. However, this difference in the load-carrying capacity becomes small, at high sliding velocities.

This difference in behavior is not fully understood at this time. A possible explanation is that the unbelted shafts permit the disks to shift slightly with respect to each other in the angular sense, even though the speed ratio

between shafts is nominally constant. That is, the indicated speed of either shaft will vary by 1 or 2 rpm at typical running speeds of 500 to 3500 rpm. On the other hand, when the shafts are belted together, there is a positive relationship between points on the two disks. Thus, any surface distress that may be initiated when two particular spots come into contact is reinforced as the same two spots continue to come into contact during successive revolutions. It is possible that a small angular shift occurring when the shafts are not belted together is sufficient to prevent two potential trouble spots from coming into contact often enough to cause failure at a lower load. Further work will need to be performed to verify this theory.

A logarithmic presentation of the data in Figure 11 is shown in Figure 12. Note that a nearly linear relationship exists







FIGURE 13. EFFECT OF SLIDING VELOCITY ON CRITICAL TEMPERATURE AT CONSTANT SUM VELOCITY V_t = 1050 ips

between log P_f and log V_s , and that the slope for the belted drive results is nearly the same as the slope for the unbelted drive results. Computing the slope of the lines yields

$$P_f \sim \left(\frac{1}{V_s}\right)^{3.88} \tag{9}$$

That is, at constant sum velocity, the scuffing failure load is inversely proportional to sliding velocity raised nearly to the fourth power.

Figure 13 shows the effect of sliding velocity on critical temperature. For the unbelted drive, the critical temperature decreases as the sliding velocity increases; for the belted drive, the critical temperature remains essentially constant. It appears that, at high sliding velocities, the two modes of operation would tend to yield approximately the same critical temperatures.

The effect of sliding velocity on the minimum elastohydrodynamic film thickness ratio at failure is shown in Figure 14. It is seen that, within a range of ± 5 percent, this ratio is nearly the same for both modes of drive and nearly constant with respect to sliding velocity.

The effect of sliding velocity on friction coefficient at failure is shown in Figure 15. It is interesting to note that the friction coefficient is not a function of sliding velocity. This is true whether the tester is operated with the belted or the unbelted drive. However, the friction coefficient is higher when the belted drive is used. Whether the higher friction coefficient for the belted drive is due to the repeated interactions between corresponding points on the two disk surfaces, as suggested earlier, remains to be established by further work.

4. Effect of Sum Velocity

The effect of sum velocity on the load at scuffing failure is shown in Figure 16. The effect of increasing V_t while holding V_s constant is to increase

the failure load. The failure load is again lower for the belted drive and, as V_t is lowered, the curves tend to merge. A logarithmic presentation of the same data is shown in Figure 17. Unlike the relationship between P_j and V_s , the slopes of these lines are different for the unbelted and belted modes of operation. Computing the slopes of the lines yields for the belted drive

$$P_f \sim (V_t)^{3.66}$$
 (10)

and for the unbelted drive

$$P_f \sim (V_t)^{1.84}$$
 (11)

The effect of sum velocity on critical temperature is shown in Figure 18. The critical temperature increases significantly with increasing sum velocity for the unbelted drive, and increases only slightly for the belted drive. As V_t is decreased, the difference between the two modes of operation becomes less prosounced.

The effect of sum velocity on the minimum elastohydrodynamic film thickness ratio at failure is shown in Figure 19. The belted and unbelted modes give about the same Λ_{mf} to within ±5 percent. However, an increase in sum velocity increases Λ_{mf} moderately.

The behavior of the friction coefficient at failure with sum velocity is shown in Figure 20. It is interesting that while f_t does not vary appreciably with V_s , it does vary with V_t , decreasing with increasing V_t for both the belted and unbelted drives. As was the case when V_t was held constant and V_s varied, the friction coefficient is higher when the belted drive is used.

5. Effect of Sliding to Sum Velocity Ratio

It would be of interest to compare the data at constant sum velocity with the data at constant sliding velocity using some factor common to both test groups. This comparison can be made only by using the ratio of the sliding velocity to the sum velocity.

In Figure 21a, the failure load is shown as a function of velocity ratio, M, for the unbelted drive. It is seen that the results for constant sliding velocity do not differ significantly from those at constant sum velocity; at M = 0.429 and 0.600, the failure load is slightly higher for the constant sliding velocity case. The results for the belted drive are shown in Figure 21b; it is seen that at M = 0.429 the failure load is also slightly



FIGURE 14. EFFECT OF SLIDING VELOCITY ON MANIMUM FILM THICKNESS RATIO AT FAILURE AT CONSTANT SUM VELOCITY V_t = 1050 ips



FIGURE 15. EFFECT OF SLIDING VELOCITY ON FRICTION COEFFICIENT AT FAILURE AT CONSTANT SUM VELOCITY $V_t = 1050$ ips



FIGURE 16. EFFECT OF SUM VELOCITY ON SCUFFING FAIL-URE LOAD AT CONSTANT SLIDING VELOCITY V_s = 350 ips



FIGURE 17. LOGARITHMIC PRESENTATION OF DATA FROM FIGURE 16







higher for the constant sliding velocity case. For both unbelted and belted drives, the failure load is seen to decrease with increasing M and the decrease is more pronounced with the unbelted drive.

Figure 22a shows the variation of critical temperature with M for the unbelted drive. The critical temperature decreases with increasing M for both the constant sum velocity and constant sliding velocity cases; at M = 0.429 and 0.600, the critical temperature is noticeably higher for the constant sum velocity case. Figure 22b presents the results for the belted drive, and it is seen that for the constant sum velocity case, the critical temperature increases slightly with M, while for the constant sliding velocity case there is a slight decrease. The variation with M is significantly more pronounced for the unbelted drive.

The variation of the minimum EHD film thickness ratio at failure with M for the unbelted drive is shown in Figure 23a. Note that at constant sum velocity, the variation in Λ_{mf} is nonmonotonic, showing first an increase as sliding velocity increases, and then a slight decrease with further increase in sliding velocity. However, at constant sliding velocity, Λ_{mf} is relatively constant. The results are shown for the belted drive in Figure 23b, and it is seen that Λ_{mf} decreases with increasing M for both constant sum velocity and constant sliding velocity tests.





FIGURE 21. EFFECT OF VELOCITY RATIO ON SCUFFING FAILURE LOAD













FIGURE 22. EFFECT OF VELOCITY RATIO ON CRITICAL TEMPERATURE

Figure 24a shows the variation of the friction coefficient at failure with the velocity ratio for the unbelted drive. The friction coefficient increases with M at constant sliding velocity; but is constant at constant sum velocity, indicating that the friction coefficient at failure is a function of sum velocity. In Figure 24b the belted drive results exhibit the same trends; that is, f_f increases with M at constant sliding velocity and remains constant at constant sum velocity.

In the practical case of a pair of gears with fixed gear ratio, the velocity ratio, M, is a function only of the local radii of curvature of the meshing teeth and will vary throughout the mesh. The speed of operation of the gear set, however, has no effect on this ratio. Thus, at any particular point along the line of action, a change in the speed of operation will affect the magnitudes of the sliding and sum velocities together; but the ratio will remain constant.

The data from Figures 21 through 24 can be presented in such a manner as to demonstrate this effect of changing the speed of operation of a pair of gears. Figure 25a shows the results for M = 0.333, which would be representative of me point along the line of action, and Figure 25b shows the results for M = 0.429, which would



be representative of another point along the line of action. The abscissa is the sliding velocity, V_s , however, the sum velocity, V_t , could have been used just as well since as either one is varied, the other changes such that their ratio remains constant. In addition to the data that were presented earlier, the results of Series XVIII, which were obtained at $V_s = 200$ ips and $V_t = 600$ ips with the belted drive, are also shown. At the top of the figure it is seen that the load at failure decreases with increasing speed for both values of M, whether the belted or unbelted drive is used. The minimum film thickness ratio at failure, Λ_{mf} , increases moderately with speed also for both values of M and for both drive modes. The friction coefficient at failure decreases vith increasing speed for both values of M and drive modes. However, the critical temperature, T_{cr} , decreases slightly with increasing speed at M = 0.333, but increases with speed at M = 0.429. The same behavior is observed for the surface temperature, T_s , in that it decreases slightly with increasing speed at M = 0.333, but increases with speed at M = 0.429.

6. Effect of Lubricant Jet Temperature

There is a marked effect of lubricant jet temperature on the scuffing failure load, at least for the one set of test conditions investigated. The results are shown in Table III. Series VIII was conducted at $T_j = 140^{\circ}$ F and Series IX was conducted at the same test conditions except with $T_j = 190^{\circ}$ F. As seen in the table, the average surface roughness of the disks, both before and after break-in, was about the same for both series. The disks tested at $T_j = 140^{\circ}$ F failed at a calculated minimum EHD film thickness about 30 percent larger than those tested at $T_j = 190^{\circ}$ F. This larger film thickness is due to the higher viscosity of the oil at the lower temperature as indicated by the thermocouple probe temperature, T_{p2} . The load at failure in the tests with $T_j = 140^{\circ}$ F was more than twice

TABLE III. COMPARISON OF AVERAGE TEST RESULTS FOR V_s = 630 ips AND V_t = 1050 ips USING LUBRICANT O-67-22 AT TWO LUBRICANT JET TEMPERATURES

Tost	T	Before bre	ak•in	_ At er	At end of break-in		At failure									
series	°F	δ <u>1</u> /δ2, μin.	δį, µin.	δ _a . µin.	h _m . μin.	۸m	δ _f . µin.	h _{mf} . μin.	^mf*	ſſ	P _f . Ib	w _e . ppi	<i>Tp</i> ² ,	rs. °F	Δ <i>Τ</i> , °F	т _{ст} . °;
	140 190	12.0/13.2 12.2/14.0	25.2 26.3	15.1 16.9	4.6 4.5	0.31 0.27	22.3 22.4	13.3 10.4	0.88 0.62	0.022 0.022	655 290	5535 3237	162 197	231 264	227 152	458 415
*Based	*Based on δ_{a} .															

that in the tests at $T_j = 190^{\circ}$ F. The average friction coefficient at failure, f_j , is identical for both series. Since friction coefficient is the same for both series, it is the higher failure load in Series VIII that accounts for the higher critical temperature.

7. Effect of Inert Environment

To determine the effect of an inert environment on scuffing behavior, the disk tester was operated in the presence of mitrogen. The nitrogen used was from a compressed-gas cylinder and contained less than 10 ppm impurities including 5 ppm O_2 , 1 ppm H_2 , 1 ppm CO_2 , 1 ppm CO_3 , 1 ppm C

Attempts were made to break in the test disks in the mert environment, using the procedure of permitting the nitrogen to flow through the system with the lubricant circulating at the break-in temperature of 90°F for 2 hi prior to beginning the break-in. However, two sets of disks scuffed during the break in process (one at 1000-lb load and another at the maximum break-in load of 2000 lb). Therefore, this procedure was abandoned and the disks were broken in in an environment of air.

The test results are shown in Table IV. For comparison, the Series XI results, obtained at the same test conditions in an environment of air, are also shown in Table IV. As shown in the table, the average surface roughness of the disks both before and after break-in are the same for both series. In nitrogen, the scuffing failure occurred at a slightly higher calculated minimum LHD film thickness ratio than in air. The friction coefficient at failure was only slightly lower for the tests with introgen, while the failure load was significantly lower. The lower load and friction coefficient at failure result in a lower calculated conjunction temperature rise, ΔT . This lower ΔT , combined with the lower surface temperature, T_s , accounts for the large difference in critical temperature.

TABLE IV. COMPARISON OF AVERAGE TEST RESULTS FOR $V_3 = 450$ ips,
$V_1 = 1050$ ips, AND $T_1 = 190^{\circ}$ F, USING LUBRICANT 0-67-22 IN
AIR AND INERT ENVIRONMENTS

100		Before bro	uk-m	At en	id of br	eak-m					At fai	lure				
series	l nyuonment	δ 1/δ 2, μm.	δ ₁ , μm.	δ ₀ . μιη.	h _m . μιη	Δ_m	δ _f . μm.	h _{mf} , μm	\mf*	ſſ	P _f . Ib	^w e. ppi	$r_{p_{1}}$	75. °₽	<u>Δ</u> Τ. [°] Γ	7 ₆₇ . 1
XI XIV	Au Nitrogen	11 8/10,7 10,6/11 7	22.4 22.4	14.3 14.2	53 5.2	0 37 0 37	16.2 18.3	9 2 10.3	0.05 0.73	0.022 0.020	1000 455	7362 4358	199 194	280 225	189 121	469 346
*Based	$t \text{ on } \delta_a$															

These tests indicate that the chemical effects of oxygen and/or moisture in the environment are important in the lubrication process, not only at the higher temperatures and speeds, but also at the lower temperatures and speeds used in the break-in process.

8. Statistical Nature of the Data

The discussion of the results thus far has been concerned with the average test results of the various series of tests rather than the individual test results within each series. It was mentioned previously that with a 10-test series, the failure load varied by a factor of two or three even though all test conditions were the same for each test. This suggests that the scuffing failures may follow a statistical distribution similar to fatigue failures.

Figure 26 shows that the scuffing failure load follows a Weibull distribution relatively well. These are the results from Series X, XI, and IX, where V_t was held constant and V_s was varied. Using these curves, or equations developed from them, one could predict with reasonable accuracy the percentage of a sample of disks that would fail at a given load.



FIGURE 26. WEIBULL PLOT OF SCUFFING FAILURE LOAD AT V_t = 1050 ips

Figure 27 presents the results for Series X, XII, and XIII, where V_s was held constant and V_t was varied. Although the results for the two higher sum velocities follow the Weibull distribution relatively well, the results for $V_t = 583$ ips do not. This could be indicative of the fact that some phenomenon that affects scuffing at the lower V_t is not present at the two higher values of V_t .

To predict the scuffing failure load over the complete range of operating variables of interest, it would be necessary to have plots similar to those shown in Figures 26 and 27, using either a Weibull or some other distribution function, covering the entire range of the variables. The results of such plots would, at best, be empirical since they do not offer the prospect of generalization. For the purpose of generalization, some suitable generalized scuffing parameter such as the critical temperature, the lubricant film thickness ratio, or some other criterion will have to be treated statistically in a similar manner. Moreover, lubricants, materials,



and the statistic the second

ALC: NO.

and factors other than operating variables alone must also be considered if a generalized scuffing parameter is being sought. Information of such broad scope is not currently available. However, statistical treatment of the data, hitherto not used in gear-scuffing work, is suggested as a possible approach once sufficient data become available.

SECTION V

CONCLUSIONS

From 90 individual scuffing tests conducted using a MIL-L-7808G lubricant (O-67-22) and test disks made from a single heat of AMS 6260 CEVM steel, run at various combinations of sliding and sum velocities, at two lubricant jet temperatures, with two methods of driving the test disks, and in air and nitrogen environments, the following conclusions are drawn:

The load-carrying capacity was found to decrease with increasing sliding velocity, increase with increasing sum velocity, decrease with increasing lubricant jet temperature, decrease with belted as against unbelted drive, and decrease in the presence of an inert environment.

The coefficient of friction at failure was found to change little with increasing sliding velocity, decrease with increasing sum velocity, change little with increasing lubricant jet temperature, increase with belted as against unbelted drive, and decrease in the presence of an inert environment.

The critical temperature was found to decrease with increasing sliding velocity at constant sum velocity, increase with increasing sum velocity at constant sliding velocity, decrease with increasing lubricant jet temperature, decrease with belted as against unbelted drive, and decrease in the presence of an inert environment. At a sliding to sum velocity ratio, M, of 0.333, the critical temperature was found to decrease slightly with increasing speed. However, at M = 0.429, the critical temperature was found to increase with increasing speed.

The minimum lubricant film thickness ratio at failure was found to change little with increasing sliding velocity, increase with increasing sum velocity, decrease with increasing lubricant jet temperature, change little with belted as against unbelted drive, and increase in the presence of an inert environment.

Judging from the very low values of the minimum lubricant film thickness ratio experienced at failure, over the entire range of all variables investigated, operation of the disks with lubricant O-67-22, a MIL-L-7808 G lubricant, has definitely extended well into the boundary lubrication regime before scuffing failure. This confirms the earlier findings in Reference 1, in which it was noted that a straight mineral oil failed as soon as the operation moved from elastohydrodynamic into a boundary lubrication regime. Two synthetic oils, however, including lubricant O-67-22, were able to operate substantially into the boundary lubrication regime without failure. This being the case, the applicability of the lubricant film thickness ratio as a generalized scuffing criterior, will require more convincing theoretical and practical justification.

The critical temperature, the more popular generalized scuffing criterion, has been found to behave differently for the belted and unbelted drives. With the unbelted drive, the critical temperature is clearly far from being constant with respect to any of the variables investigated herein. On the other hand, the critical temperature remained fairly constant with the belted drive, for which it was only possible to study the effects of sliding and sum velocities over a rather limited range. The nonconstancy of the critical temperature for the unbelted drive with respect to all operating variables, its contrasting behaviors with respect to the method of drive, and especially its lack of consistent behavior with respect to speed at two different sliding to sum velocity ratios, cannot be explained by any conventional theoretical justification of the critical temperature hypothesis. Even as a strictly arbitrary scuffing criteria, its generalized relationship with all operating variables has yet to be developed. This relationship cannot be accomplished with confidence without a great deal of additional research.

APPENDIX I

DETERMINATION OF CONSTANTS IN EQ. (6)

Since, by definition, the disk friction torque, T_f , as measured by the two torquemeters must be equal, Eq. (2) may be subtracted from Eq. (1) to give

$$T_{r1} - T_{r2} = T_{m1} + T_{m2} \tag{12}$$

From Eq. (5), for the upper shaft

$$T_{m1} = aP + bN_1\mu_j^c \tag{13}$$

and for the lower shaft

$$T_{m2} = aP + bN_2\mu_i^c \tag{14}$$

where

Sand State and a state water -

 T_{m1} - machine-loss torque for upper shaft, in.-lb

 T_{m2} – machine-loss torque for lower shaft, in.-lb

 N_1 – upper shaft speed, rpm

 N_2 – lower shaft speed, rpm

and other symbols were defined after Eq. (5). Now substituting Eqs. (13) and (14) into Eq. (12)

$$T_{r1} - T_{r2} = 2aP + bu_i^c (N_1 + N_2) \tag{15}$$

But N_1 can be related to N_2 by the ratio of the shaft rotational speeds, $S = N_1/N_2$, so that Eq. (15) becomes

$$T_{r1} - T_{r2} = 2aP + (S+1)bN_2\mu_i^c \tag{16}$$

To determine the constant a, let S, N_2 , and μ_j be held constant and the load be varied between two values, say P and P'. Then for load P, Eq. (16) gives

$$(T_{r1} - T_{r2}) = 2aP + (S+1)bN_2\mu_j^c \tag{17}$$

or rearranging

$$(S+1)bN_2\mu_i^c = (T_{r1} - T_{r2}) - 2aP$$
(18)

where $(T_{r1} - T_{r2})$ is the difference in torquemeter readings corresponding to load P. For load P', Eq. (16) gives

$$(T_{r1} - T_{r2})' = 2aP' + (S+1)bN_2\mu_j^c$$
⁽¹⁹⁾

or rearranging

$$(S+1)bN_2\mu_i^c = (T_{r1} - T_{r2})' - 2aP'$$
⁽²⁰⁾

where $(T_{r1} - T_{r2})'$ is the difference in torquemeter readings corresponding to load P'. Equating Eqs. (18) and (20), and solving for the constant a, one obtains:

$$a = \frac{(T_{r1} - T_{r2})' - (T_{r1} - T_{r2})}{2(P' - P)}$$
(21)

To determine the constant c, the data from two runs are needed where P and N_2 are the same, but the lubricant viscosity is varied from μ_j to μ'_j . Then for viscosity μ_j , Eq. (16) gives

$$(T_{r1} - T_{r2}) = 2aP + (S+1)bN_2\mu_j^c$$
⁽²²⁾

or rearranging

$$(S+1)bN_2 = \frac{(T_{r1} - T_{r2}) - 2aP}{\mu_i^c}$$
(23)

where $(T_{r1} - T_{r2})$ is the difference in torquemeter readings corresponding to the case where viscosity is μ_j . For viscosity μ'_j , Eq. (16) gives

$$(T_{r1} - T_{r2})' = 2aP + (S+1)bN_2\mu_i^{c}$$
⁽²⁴⁾

or rearranging

$$(S+1)bN_2 = \frac{(T_{r1} - T_{r2})' - 2aP}{\mu_i'^c}$$
(25)

where $(T_{r1} - T_{r2})'$ is the difference in torquemeter readings corresponding to the case where viscosity μ'_{j} . Equating Eqs. (23) and (25) and rearranging

$$\left(\frac{\mu_j}{\mu_j'}\right)^c = \frac{(T_{r1} - T_{r2}) - 2aP}{(T_{r1} - T_{r2})' - 2aP}$$
(26)

Taking the logarithm of both sides

$$c \, \ln\left(\frac{\mu_j}{\mu_j'}\right) = \ln\left[\frac{(T_{r1} - T_{r2}) - 2aP}{(T_{r1} - T_{r2})' - 2aP}\right]$$
(27)

or

$$c = \frac{\varrho_n \left[\frac{(T_{r_1} - T_{r_2}) - 2aP}{(T_{r_1} - T_{r_2})' - 2aP} \right]}{\varrho_n \frac{\mu_j}{\mu_j'}}$$
(28)

To determine the constant b, Eq. (16) may be written

$$b = \frac{(T_{r1} - T_{r2}) - 2aP}{(S+1)N_2\mu_i^S}$$
(29)

from which b may be calculated since all other quantities are now known.

APPENDIX II

SUMMARIES OF TEST RESULTS

The results of all tests performed during this reporting period are summarized in this appendix. Each table contains the individual results of a series of replicate tests conducted under one set of conditions, as well as the average results of the series. Summaries of the results of Series I through VII. conducted with lubricants B, 0-64-2, and 0-67-22, together with Batch A of AMS 6260 steel test disks, were presented in a previous report.⁽¹⁾ This appendix presents only the results of Series VIII through X¹.¹H, conducted with lubricant 0-67-22 in combination with Batch B of AMS 6260 steel test disks.

The break-in and test conditions, as well as other pertinent details about each series, are shown at the top of each table. Unless otherwise specified, the series was conducted in an air environment and with the unbelted drive system.

Break-In Conditions. As is customary, the break-in process involves operating the test disks through a step-load sequence, at a sufficiently low lubricant jet temperature and at sufficiently low sliding and sum velocities, in such a manner that, while no scuffing should occur during the break-in, the final or maximum break-in load should be higher than the anticipated scuffing failure load to be obtained later during the test. The rationale behind this procedure is that if the failure load obtained during test should be higher than the maximum break-in load, then the failure could occur at the edge, i.e., on the outside, of the broken-in track so that this failure load could be lower than it would have been had it occurred inside of the broken in track. We have followed this break-in procedure throughout this program, but have found that edge failures still occasionally occur. These edge failures are believed to be caused by the shape of the constriction in the conjunction due to the effect of side flow in the conjunction.

As shown at the top of the tables, all break-ins were conducted at the same lubricant jet temperature of 90° F. However, the break-in speed conditions and the load schedule were not strictly the same for all series. In the interest of brevity, the break-in load schedule is shown in an abbreviated form. For example, the notation $4 \times 300(15)$ indicates four load steps of 300-lb increments, each step being run for 15 min.

For Series VIII and IX, the break-in conditions were identical. The maximum break-in load of 1200 lb exceeded all observed failure loads. When Series X was begun, the same load schedule and speed conditions were used for Test B24. In this test, failure occurred at 900 lb, which is less than the maximum break-in load of 1200 lb. For the next two tests, B25 and B26, the failure load exceeded this maximum break-in load. It appeared from the observed failure loads that a 3000-lb maximum break-in load might be more appropriate. This 3000-lb load was implemented for Test B27, however, the shaft speeds had to be increased to prevent the disks from chattering at the high loads. In the next attempt at break in (Test B28, which was discarded), the disks scuffed at the 3000-lb load, even with the increased shaft speeds. It was then decided to reduce the maximum break-in load to 2000 lb, but to continue using the higher shaft speeds. From Test B29 on, the break-in was standardized with $V_s = 47$ ips, $V_t = 141$ ips, with a maximum break-in load of 2000 lb achieved in four increments of 500 lb, each run for 15 min. No difficulties were encountered with break-in thereafter.

Test Conditions. The test conditions were varied for each series as noted at the top of each table. However, the test load schedule, designated as 50(3) herein, was the same throughout the tests. After the speed conditions were reached, increments of 50 lb of load were applied, allowing 3 min at each load step to achieve equilibrium, until scuffing failure was detected by the operator. The "Mode" column in the tables shows the mode or indication of failure (CR for contact resistance, T for torque increase). Each failure was verified by visual inspection after stopping the test.

Other Symbols. As for the various quantities tabulated, most have already been discussed, and all are defined in the List of Symbols. The metal monitor reading (mm) is shown before break-in (BBI), after break-in (ABI), and after test (AT). The first value is for the upper disk and the second is for the lower disk. The metal monitor reading itself does not yield any quantitative information about the character of the disk surface, but a change in metal monitor reading may suggest a change in the surface qualities of the disks.⁽¹⁾ For example, the formation of a surface film due to chemical reaction between the lubricant and metal surface can generally be detected by a change in the metal monitor reading.

The tables also show the composite surface roughness of the pair of disks before break-in (δ_i) , after break-in (δ_a) , and after failure (δ_f) , as well as the film thickness ratios at the end of break-in (Λ_m) and at failure (Λ_{mf}) . Details of calculations are given in Reference 1. It should be pointed out that the film thickness ratio at failure, as used in this report, has been calculated using the composite surface roughness after break-in. This quantity should preferably be based on the composite surface roughness just before, rather than after, failure since it is intended to indicate the degree of asperity interaction occurring just before failure. Moreover, in most cases, the scuffing failure extends completely around the periphery of the disks so that δ_f is usually higher than δ_a and cannot, in general, be accurately measured. Therefore, rather than using the questionable δ_f , the tabulated Λ_{mf} has been calculated using δ_a in all cases.

TABLE V. SUMMARY OF RESULTS FOR SERIES VIII

Test lubricant: O-67-22

and the second
Ē

Hallock 1. Sta

Test disks: AMS 6260 (Batch B)

Break-in conditions: $T_j = 90^{\circ}F$, $V_s = 24$ ips, $V_t = 70$ ips, $4 \times 300(15)$ Test conditions: $T_j = 140^{\circ}F$, $V_s = 630$ ips, $V_t = 1050$ ips, 50(3)

Taul		mm		Before brea	ik in	Aten	d <u>othr</u>	tak in_			_		A	failore					
no	BBI	AB1	лt	δ ₁ /δ ₂ . μm.	δ ₁ . μιη.	⁶ а. µіп,	h _m . μιπ	۸m	δ _f , μιη,	հ _m , µո	^mí*	ŋ	P _f . Ib	ме. ррі	1 _{p2} .	T₅ °Γ	41. °1	T _{cr} , °I	Mode
B3 B4 B5 B6 B7 B9 B10 B11 B12 B13	1 0/0 5 2 0/1.0 1 0/1 2 0 0/1 6 1 5/2 5 2 5/0 5 2.3/1 6 2 7/3 2 3 3/2 0 2 0/2 5	3 0/2 2 3 0/3 0 3 2/3 0 2 0/3 2 3 0/3 3 3 0/2 0 3 2/2 8 3 0/3 0 3 4/2 2 2 5/2 5	2 0/1 2 2 5/2 3 1 5/2 0 1 2/2 2 1 5/2 6 1 5/1 0 2 0/1 6 1 0/2 5 2 5/1 0	11 7/14.5 12 6/16 3 13 1/12 3 11 7/12 9 14 3/15 1 11 2/16 2 10 6/12 4 9 9/10 3 13 0/13 4 12 2/6 6	26 2 28 9 25 4 24 6 29 4 21 4 21 0 20 2 26 4 20 8	14 4 15.2 14 7 13 6 14 9 15 5 15 8 15 0 15 8 16 1	46 46 46 45 43 47 46 46	0 32 0 30 0 31 0 34 0 30 0 31 0 30 0 31 0 29 0 29	21 4 21 4 23 4 20 2 20 4 22 7 22 0 20 3 25 3 25 5	132 135 98 117 144 140 140 126 147 153	0 92 0 89 0 67 0 86 0 97 0 91 0 89 0 84 0 93 0 55	0 022 0 021 0 022 0 022 0 024 0 024 0 024 0 021 0 018 0 024 0 022 0 022	450 450 1090 950 500 650 650 900 500 500	4355 4355 7416 7167 4672 5565 5565 5565 6913 4672 4672	164 162 191 171 154 155 155 163 152 148	243 234 357 271 200 203 203 237 191 174	190 183 287 288 225 220 191 290 207 189	433 417 644 559 425 423 394 527 198 361	CR CR CRJ CRJ CRJ CRJ CRJ CRJ CRJ CRJ CR
			Average	12.0/13.2	25.2	151	46	031	22.3	13.3	0 88	0.022	655	5535	162	231	227	458	
			Sid dev	1 3/2 5	31	0.8	01	0.01	20	16	0.08	0.000	217	1207	12	53	44	89	
*Base	d on $\delta_{\mathbf{a}}$																		

TABLE VI. SUMMARY OF RESULTS FOR SERIES IX

Test lubricant: O-67-22

Test disks: AMS 6260 (Batch B) Break-in conditions: $T_j = 90^\circ F$, $V_s = 24$ ips, $V_t = 70$ ips, $4 \times 300(15)$ Test conditions: $T_j = 190^\circ F$, $V_s = 630$ ips, $V_t = 1050$ ips, 50(3)

Tea		mm		Before bre	ak-in	At en	d of br	cak-in					٨	t failure					
no	BBI	ARI	лі	8 ₁ /8 ₁ µm.	δ ₁ , μιη,	λ ₃ , μιη	հ _m , µւո,	۸m	λ μιη	հ _m , µւո	∿mt*	٩	Р ₇ . В	we. ppi	1p2, 1	1 <u>5</u> "I	ΔΙ. Ί	1 _{ct} . 1	Mode
B14 B15 B16 B17 B18 B19 B20 B21 B22 B23	1 2/1 11 0 5/2 0 2 5/2 0 1 0/0 6 1.0/0 0 0 5/2 5 1 5/2 5 1.5/1 5 2 0/1 0 1 0/1 5	1 8/2 0 1.2/3 0 2 2/2 0 1.0/1 5 1.0/0 0 0 5/2 5 1.4/2 6 1.8/1 5 1.5/0 8 2.0/2 5	1,0/0 5 0 5/2.5 1 3/1 6 1,2/2 0 1 8/0 0 0 5/2 0 1 5/2 6 2,2/2 0 2,0/1,0 0 5/1,2	9.4/12.8 11.3/14.1 10.7/12.4 12 2/14.6 11.6/12.9 12 8/1 ⁻⁰ 13.1/16.2 12 9/16.5 12.5/15.3 15.9/12 3	22.2 25.4 23.1 26.8 29 5 25 8 29.3 29.4 27.8 28.2	16.1 17 6 16,8 17.2 19.1 16 6 17.0 18,9 15,4 14,5	46 45 45 45 46 46 45 45 45 45	0 29 0 26 0 27 0.26 0.24 0.28 0 26 0.24 0.29 0.29 0.32	23,4 22,2 22,5 24,0 21,7 20,9 22,3 24,3 22,0 21,0	100 105 103 103 105 103 101 105 103 108	0 62 0 60 0 61 0 60 0 55 0 62 0 60 0 56 0 67 0 74	0.022 0.025 0.025 0.021 0.022 0.020 0.020 0.020 0.020 0.022 0.022 0.025	400 250 300 250 300 250 300 250 350 250 300 200	4026 2943 3323 3323 2943 3323 2943 3683 2943 3323 2536	199 197 197 198 197 198 197 198 197 197	276 260 263 270 256 270 270 260 260 253	182 162 148 159 130 156 150 127 153 149	458 422 411 429 386 426 420 387 413 402	CR.I CR.I CR.I CR.I CR.I CR.I CR.I CR.I
			Average	12 2/14.0	26.3	16.9	45	0 27	22.4	104	0.62	0 022	290	3237	197	264	152	415	
		L	Std dev	1.7/1.6	2.5	1.4	01	0.02	12	02	0.0\$	0.003	57	422	1	7	16	21	
.B746	tion δ_{1}																		

TABLE VII. SUMMARY OF RESULTS FOR SERIES X

Test lubricant: O-67-22

State of the state

いたがないというともの人口を行いたがいたのでしたかのとないであるとなった。

the same of the New York is the industry of the second strategies in the March of the Strategies in the second

Test disks: AMS 6260 (Batch B)

Bieak-in conditions: $T_j = 90^\circ F$, $V_s = 47$ ips, $V_t = 141$ ips, $4 \times 500(15)$ Test conditions: $T_j = 190^\circ F$, $V_s = 350$ ips, $V_t = 1050$ ips, 50(3)

		mm		Before bre	ak-in	Aten	d ot bre	ak-in						t failure					
Test no,	B81	ABI	AI	δ,/δ ₃ , μιπ	δ ₁ , μιπ	δ _a , µm	հ _m . µյո	۸m	ծր. µm	h _m , μιπ	∆ _{mf} *	ł	P _f . 16	^м ен ppi	¹ ρ2.	Iş, °F	∆1. °ŀ	1 _{cr} , °}	Mode
B24 B25 B26 B27 B29 B30 B31 B32 B33 B34	0 0/1.3 1.3/1 5 0 5/1.5 0 0/1 0 0 5/0 2 0 0/1.3 1 5/1 5 0 0/1.0 1.5/1.5 1.6/2 5	-1,0/2 0 0 4/1 0 0 2/0.5 0 5/0 8 1 0/0 5 0 5/0 2 1.2/2.5 0 8/2 0 2 0/3 2 3,0/3.2	-2.0/-0 5 2.0/1.6 1.0/0.5 0.0/0 2 1.5/1 6 2.5/2 5 2.0/1.0 2.0/1.5 2.2/2 0 Average	16.1/17.6 10.4/11.7 11.7/10.1 14.6/11.4 11.5/12.2 10.0/10.5 10.0/12.6 11.2/13.1 11.0/11 11.8/14.3 11.8/14.3	33 7 22.1 21.8 26.0 27.7 20 5 22 6 24.3 22 1 25 9 24 5	16.6 14 4 15 8 13.2 12.3 13 1 11.5 13.3 12 6 13 6 13 6	4,6 4,6 4,5 4 2 5 4 5 2 5 2 5 2 5 2 5 2 5 2 4 9	0 28 0 32 0.28 0 32 0 44 0 40 0 45 0 39 0 41 0 38 0 36	38.3 17 7 16 8 19 1 15 1 14 2 13 9 16 1 16 7 14 0 18 7	93 7.8 76 68 8.1 75 71 7.5 77 73 77 73	0 56 0 54 0 48 0 51 0 66 0 57 0 62 0 56 0 61 0 54	0 030 0 022 0 022 0 023 0 023 0 023 0 023 0 019 0 018 0 020 0 022 0 020 0 022 0 022	900 2,950 3,300 2,300 2,150 3,450 4,350 3,300 3,000 >3,700 2,940	6,9:3 15,254 16,438 12,922 12,354 16,932 19,762 16,438 15,426 17,741 14,715 3,653	198 208 210 231 206 217 216 213 209 215 212 9	270 370 390 603 35 ³ 40t 453 420 380 436 -405	194 255 271 238 226 24 ⁻ 2- 253 254 262 243	464 625 661 841 579 653 705 673 634 >698 648 648	CR.J CR.J CR.T CR.T CR.T CR.T CR.T CR.T CR.T CR.T
•Base	d on 63		510, 000.			1	l. ° •		1				<u> </u>		<u> </u>		L	•	

+Support bearing faned. Test terminated before wulti

TABLE VIII. SUMMARY OF RESULTS FOR SERIES XI

Test lubricant: O-67-22

Test disks: AMS 6260 (Batch B) Break-in conditions: $T_j = 90^\circ F$, $V_s = 47$ ips, $V_t = 141$ ips, $4 \times 500(15)$ Test conditions: $T_j = 190^\circ F$, $V_s = 450$ ips, $V_t = 1050$ ips, 50(3)

		៣៣		Before bre	ak in	Aten	d of br	e da in					A	Janlure_					
Test no	BBI	ABI	AT	δ ₁ /δ ₁ . μιπ	δ _ι , μιη	^л µ1П	h _m . μιπ	۱m	¢⊦ µin	h _m , µn	`mf*	4	P _f . Ib	₩., врі	1 _p ?	Т. 1	دا ۱	1 _{cr} 1	Mode
B35 B37 B38 B35 B40 B41 B42 B43 B43 B44 B44 B45	0 0/1 0 2 5/1 0 1 2/1 0 1 0/1.2 -1 0/0 5 0 5/1 2 1.5/0 5 1 2/1 0 1 9/0 9 2 0/1 7	0 0/1 5 2.2/2 0 2.0/1 6 1 6/1 5 0 5/2 0 2 0/1 0 1 5/0 0 6 6/1 2 0 5/0 0 0 6/1 0	0.5/1 0 2 5/1 0 1 5/1.2 1.2/1.5 0 6/1.5 2 0/1 2 1 6/0 5 1 0/1 8 1 5/1 0 1 9/2 0	16.7/13 6 8.6/10 5 10.5/9 9 9.3/10.5 14.6/11.7 13.6/11 1 11.7/10.1 13.2/7 7 9.0'9 9 9.8/11 6	36-3 19,1 20-4 20-3 26,3 24-7 21-5 20-9 18-9 21-4	16 9 15 1 15 6 13 7 14 8 16 0 14 0 12 6 11 7 14 0	52 51 52 55 52 52 51 53 56	0 41 0 34 0 35 0 38 0 37 0 33 0 37 0 43 0 44 0 45 0 45	195 153 149 147 181 176 169 142 142 164	100 92 93 86 95 95 85 85 85 85	0 59 0 61 0 64 0 63 0 64 0 57 0 68 0 70 0 76 0 65	0 026 0 021 0 021 0 022 0 022 0 023 0 022 0 023 0 020 0 021	\$59 1100 1000 1450 708 900 850 1250 1150 1050	4978 70-3 7410 9501 5547 6913 6655 8606 8140 7661	194 198 197 203 198 200 196 202 202 202 200	230 270 260 320 270 290 253 310 310 290	132 193 190 238 165 189 178 228 190 188	362 463 473 558 435 479 431 538 500 478	(R,1 (R,1 (R,1 (R,1 (R,1 (R,1 (R,1 (R,1
			Average	11 8/10 7	22.4	14.5	53	0 37	16.2	92	0.65	0.022	1000	7362	199	280	183	469	
			Std dev	2 7/1 5	36	15	0.2	0.04	18	04	0.06	0.001	263	1325	1	2)	- 346	56	
•Base	donsa																		

TABLE IX. SUMMARY OF RESULTS FOR SERIES XII

Test lubricant: O-67-22

Test disks: AMS 6260 (Batch B)

Break-in conditions: $T_j = 90^\circ F$, $V_s = 47$ ips, $V_t = 141$ ips, $4 \times 500(15)$ Tes

st c	onditions:	Тj	=	190°	Ŧ,	v,	Ξ	350	ips,	vt	=	81	7	ips,	50(3)
------	------------	----	---	------	----	----	---	-----	------	----	---	----	---	------	------	---

Test		mm	_	Before hie	<u>uk in</u>	Aten	d of br	of break in At failure											
nu	881	АВІ	AT	۸, ۸, µn	⁵ լ ուպ	δ ₃ . μιπ	հ _m . µտ	۸m	δ <u>Γ</u> . μιη	հ _ո ւ µւո	^mf*	fi -	Р ₁ . 16	Me. Ppi	Г _Р 2. 1	ן, ו	ג. 1`	1 _{er} , 1	Mode
846 847 848 849 850 851 853 854 855 856	1 2/0 5 1 2/0 5 1 0/3 5 1 0/3 5 1 0/2 5 1 0/2 0 1 2/1 0 1 6/1.6 1 0/1 0 2 8/0 5 1 0/1 5	0 5/0 5 2.0/1 6 2 0 ⁴ 2 0 1.2/2 0 2 4/2 4 2 4/2 0 2 4/1 5 1.5/1 0 3 2/2 0 1.8/2 2	1 2/08 1 8/1 6 1.8/2 2 1 2/2 0 2.2/2 5 2.5/2 0 2.2/1 5 1.5/1 5 2 2/0 8 1 2 1 0	8 4/9 7 10 4/12 8 1+ 1/6 4 9 1/11 5 9 7/8 7 8 6/12 3 12 0/10 7 9 2/12 8 7 1/10 3 12 1 12 1	18 1 23 2 17 5 20 6 18 4 20 9 22 7 22 0 17 4 24 2	106 134 141 145 123 140 169 159 124 138	53 52 54 53 51 55 55 60 55	0 50 0 39 0 38 0 37 0 41 0 36 0.33 0 35 0 48 0 40	116 155 150 153 160 162 16.7 15.2 159 184	75 75 77 82 77 83 77 76 76	071 056 055 057 062 059 036 048 061 056	0 022 0 022 0 024 0 023 0 021 0 026 9 025 0 024 0 019 0 020	1350 1300 1100 750 1300 650 1150 1200 1300 1300 1150	9059 8833 7903 6122 8833 5565 8140 8374 8374 8833 8140	198 199 197 194 197 195 197 198 198 198	273 276 260 230 256 236 260 270 270 253	203 195 193 156 190 163 208 208 174 167	477 471 453 386 446 399 468 478 444 420	T CR,I CR,I CR,I CR,I CR,I CR,I CR,I CR,I
			Average	9 8/10 7	20.5	138	54	040	156	78	0 57	0.023	1125	7980	197	258	186	444	
*Base	tonó,		Std dev	16/20	23	18	03	0.06	11	03	0.07	0.002	240	1194	4	14	19	-17	

TABLE X. SUMMARY OF RESULTS FOR SERIES XIII

Test lubricant: 6 67-22

Test disks: AMS 6260 (Batch B)

Break-in conditions: $T_J = 90^{\circ} F$, $V_S = 47$ ips, $V_t = 141$ ips, $4 \times 500(15)$ Tes

st conditions: $T_j = 190^{\circ}F$, $V_s = 350^{\circ}F$	$1p_{x}, V_{t} = 583 \text{ (p_{x}, 50(3))}$
--	--

Tau		mm		Below bre	ak in	V en	d of br	eak in			_			t failure					
no	BBI	AB1	Al	15. µm	^. μm	`s µm	հա. ԿՍ	۱m	^ر חוµ	հ _m . µin	\mt*	11	P1 19	₩¢ PPi	1	<u> </u>	۱د ۱	1., 1	Mode
858 859 860 861 862 863 864 864 864 865 866 867	1 0/2 0 1 2/1 5 0 8/1 2 1 0/1 8 0 2/0 0 1 0/0 6 0 6/0 5 0 5/1 5 1 0/0 5 1 0/2 0	$\begin{array}{c} 2016 \\ 1040 \\ 0202 \\ 0505 \\ 00406 \\ 2016 \\ 1005 \\ 1222 \\ 1240 \\ 1518 \end{array}$	1620 1220 1515 1515 1406 1510 1020 0512 1020	124 89 84 102 114 130 105 165 104 148 116 152 103 107 143 153 124 108 147 91	24 0 48 3 24 4 26 9 26 9 29 6 23 2 20 8	11 " 12 1 13 7 14 1 15 8 15 8 15 8 13 9	<pre></pre>	0-44 0-44 0-38 0-37 0-36 0-37 0-31 0-37 0-31 0-35 0-35 0-35 0-35 0-35 0-35 0-35 0-35	183 170 169 207 169 232 205 241 191 162	77 72 75 77 66 77 65 64 71 74	066 039 033 033 047 049 047 041 047 041 045 045	0 023 0 023 0 032 0 036 0 027 0 036 0 027 0 023 0 023 0 024 0 032 0 033	150 250 150 150 550 150 650 250 250	2094 2543 2094 2094 4975 2094 4975 2094 5565 5847 2913 2536	193 193 192 190 495 191 194 197 191 192	193 220 213 193 236 196 233 260 200 210	87 112 120 133 195 144 172 192 154 140	280 332 333 326 431 340 405 452 364 360	(R.1 (R.1 (R.1 (R.1 (R.1 (R.1 (R.1 (R.1
			And der	11 3 12	23.6	114 13	· · ·	1113* 1114	193 28	• <u>:</u> 	032 007	0.029	320 223	test esta	192	213 22	145 35	360	
· Bave	d on A ₃													-					

TABLE XI. SUMMARY OF RESULTS FOR SERIES XIV

Test lubricant: O-67-22

.

Test disks: AMS 6260 (Batch B)

Break-in conditions: $T_j = 90^\circ F$, $V_s = 47$ ips, $V_t = 141$ ips, $4 \times 500(15)$ Test conditions: $T_j = 190^\circ F$, $V_s = 450$ ips, $V_t = 1050$ ips, 59(3), nitrogen environment

				Defension												- 1			
Test		F	r	A /A.	AL-IN A.	Aten	1 h.	Cak-in	1	h		r	E Pr	w.	T	Τ.	AT	TT.	l
no	BBI	ABI	AT	μin,	μin.	ип.	μin	^m	μin	ייתיי עוע	^mf*	4	tb	DD1	: **	۰ř	*p	₩.	Mode
		┫━━ ━━━━━							·						<u> </u>		<u> </u>	<u> </u>	
B68	1.2/0 0	20,1.0	0 5/0 0	9.9/11.4	21.3	14.1	5.1	0.36	16.6	99	070	0.022	600	5276	195	240	151	391	CR,T
B69	0.8/1.0	1.0/1.5	0 5/0 5	91/141	23.2	14.3	51	0 36	18.8	10.8	0 76	0.023	250	2943	193	220	100	320	CR,1
B70	1.2/0.5	1.5/1.8	1 0/1.0	12.3/11 6	239	(5.1	5.2	0.34	19.3	101	0.67	0.023	500	4672	124	230	143	373	CR.T
B72	3.0/2.0	3 2/2 5	2 0/1.8	11.2/11.9	23.1	100	5.1	0.32	20.4	10.8	0.67	0.019	300	3323	192	210	93	303	CR,T
B73	1.2/1.2	1.8/1.5	1.5/10	13.5/11.4	24,9	14.6	51	0 35	189	100	0 69	0 024	550	4978	194	230	155	385	CR,T
B76	1.0/1.5	1.2/1.0	1.0/0.5	13.0/10.0	230	136	5.5	0.40	191	10.4	0 76	0.018	400	4026	193	220	101	321	CR,T
B77	3.1/2.5	2 6/2.5	2.3/1.8	9.7/15.7	25.4	14,4	5.2	0 36	171	10.3	0.72	0.019	450	4355	193	220	112	330	CR.I
878	1.6/1.2	1.5/2.0	1.0/2.0	8 4/5.8	14.2	130	52	0.40	17.2	10.2	0 79	0.019	500	4672	193	216	118	334	CRJ
B79	1 8/1.0	1.0/1.5	1.0/0 5	\$ 8/14.4	23,2	121	53	044	171	105	0 86	0.017	450	4355	191	200	99	299	CR,1
B80	0 5/0 5	1.5/1.2	0.5/0 5	10 4/11.1	21.5	14.6	53	0.36	189	98	0 67	0 0 20	550	4978	197	260	135	395	CR,T
			Average	10 6/11.7	22.4	14 2	5.2	0 37	18 3	10 3	0 73	0 0 2 0	455	4358	194	225	121	345	
			Std. dev	1 8/2.7	3.1	1.1	01	0.04	12	03	0.06	0.002	112	745	2	17	23	37	
*Base	d on ŝ _a .																		

TABLE XII. SUMMARY OF RESULTS FOR SERIES XV

Test lubricant: O-67-22

Test disks: AMS 6260 (Batch B)

Break-in conditions: $T_j = 90^\circ F$, $V_s = 47$ ips, $V_t = 141$ ips, $4 \times 500(15)$ Test conditions: $T_j = 190^\circ F$, $V_s = 350$ ips, $V_t = 1050$ ips, 50(3), belted drive

T		mm		Before bre	al.in	Ater	d of br	eak-in					4	tailure					
no	BBI	ABI	AT	δູ/δ ₃ , µຫ	δ ₁ . μη	δ ₃ . μπ	հ _m . µյո	۸m		h _m , µin	۵ _{mf} •	4	Р _Г . 16	we PPI	162	1,. 1	اد ۱	T _{cr} l	Mode
881 882 833 884 886	1 0/1 8 1.6/0 5 1.6/1 6 2 0/0 0 0.5/2 0	1 7'2 0 20/, 2 20/2 0 1.8/0 6 0 6/1 8	1.5/2 1 1.8/1 1 2 0/2 5 1 3/0 5 1 0/2 2	8.3/11.8 9.8/14.0 11 6/12 2 11 2/9 4 9 5/9 4	20 1 23.8 23 8 20 6 18 9	16 2 14 5 16 0 15 1 13 0	53 46 5.2 53 55	0 33 0 32 0 33 0 35 0 43	46 1 13 8 14 9 14 0 47 8	99 91 94 95 97	U 61 0 62 0 59 0 63 0 74	0 023 0 626 0 627 0 625 6 624	650 1150 850 800 750	5565 8140 6655 6391 6122	194 199 197 196 196	226 280 260 253 246	126 193 169 155 142	352 473 429 408 385	(R (R (R (R (R
			Average	10.1/11 4	21.5	150	52	0.35	27.1	95	064	0.025	840	6575	196	253	152	410	
*Base	d on S _a	I	Sta des	1 3/2 0	12	<u> </u>	03	0.04	179	03	0.06	0.001	185	964		20		4	

TABLE XIII. SUMMARY OF RESULTS FOR SERIES XVI

Test lubricant: O-67-22

Test disks: AMS 6260 (Batch B)

Break-in conditions: $T_j = 90^\circ F$, $V_s = 47$ ips, $V_t = 141$ ips, $4 \times 500(15)$

Test	conditions: T _j :	= 190°F, V	_s = 450 ips,	V _t =	1050 ips.	50(3).	belted	drive
------	------------------------------	------------	-------------------------	------------------	-----------	--------	--------	-------

тал	mm			Before break in		At end of break in			At failure											
по	8BI	ABI	AI	δ ₁ /δ ₂ , μιη	δ ₁ . μιπ	δ _a . µm	h _m . μιπ	Λ _m	۸£ سالم	հ _m , µn	^mf*	મ	Pf. Ib	w _е . ppi	¹ p².	ь, Т	اد. ۱	1 ₆₁ . 21	Mode	
887 888 889 890 891	2 0/1.0 1.8/1.0 1 0/0 0 1.0/2 0 1 5/0 0	2.2/1 6 1 8/1 0 2 0/1 5 1 5/1.8 1.8/1 2	1.5/1 0 1 3/0 5 2 0/1.5 1.3/2 0 2 0/1 0	11 0/12 6 12 1 13 3 9 1/17 9 12.1/13 1 15 0/12 8	236 254 275 252 278	15 5 17 8 19.2 14 9 17 9	55 56 55 58 52	0 35 0 31 0 28 0 33 0 29	154 177 180 174 176	96 103 100 103 104	0 62 0 58 0 52 0 69 0 58	0 025 0 025 0 026 0 023 0 023	650 350 350 300 300	5565 3683 3683 323 3323	198 196 199 197 196	270 253 233 263 253	178 134 136 113 131	448 387 419 376 384	(R (R (R (
			Average	12 0/13 9	25.9	171	55	0 Y	172	10 1	0.60	0.025	390	3915	197	264	138	423		
			Std des	2 0/2.2	18	18	0.2	0.03	10	03	0.06	0.001	148	940	I	в	24	46		
*Base	*Baved on 6a																			

TABLE XIV. SUMMARY OF RESULTS FOR SERIES XVII

Test lubricant: 0-67-22

Ĩ

Vice of the second

ないないできたいないのできる

°~₹

Such 25 See

Test disks: AMS 6260 (Batch B)

Break-in conditions: $T_j = 90^\circ F$, $V_s = 47$ ips, $V_t = 141$ ips, $4 \times 500(15)$ Test conditions: $T_j = 190^\circ F$, $V_x = 350$ ips, $V_t = 817$ ips, 50(3), belted drive

lest no	ពរ៣			Before break in		At and of break-in			\t failure										
	881	ABI	AT	δ, λ ₂ . μιπ	', μιπ	د^ חוµ	h _m µın	۱m	້າ ມເກ	հ _{ու} µո	∿m1*	'1	197 198	ppi We	1 ₁ 2	L, F	اد ا	1.,	Moste
892 893 894 895 896	1520 2645 2020 25422 0548	18/18 2015 1820 1324 10/20	2020 30·20 22·22 2526 1520	10350 176/105 9891 12296 110/123	18 3 28 1 18 9 21 8 23 3	138 164 142 158 155	64 68 64 62 58	() 39 () 35 () 39 () 39 () 39	14 5 16 7 13 7 15 7 14 9	5 5 5 9 7 5 5 5 9 7	0.62 0.52 0.58 0.58 0.49	0.025 0.029 0.024 0.028 0.028	440	-16" 25.36 43.62 43.62	195 195 195 194 201	236 246 247 250 290	130 147 146 94 238	160 191 191 194 194 194	(R (R (R (R
	-		Average	12 2/9 9	22.1	151	57	0 38	151	84	4.16		1.20	46.53	146	. 45	142	146	
			Std dev	32/16	19	11	03	0.02	12	u C	aux	0.002	280	<u>, , , , , , , , , , , , , , , , , , , </u>	 		34	63	
Base	don 6,																		

TABLE XV. SUMMARY OF RESULTS FOR SERIES XVIII

.

Test lubricant: O-67-22 Test disks: AMS 6260 (Batch B) Break-in conditions: $T_j = 90^\circ F$, $V_s = 47$ ips, $V_t = 141$ ips, $4 \times 500(15)$ Test conditions: $T_j = 190^\circ F$, $V_s = 200$ ips, $V_t = 600$ ips, 50(3), belted drive

Test	mn.			Before break-in		Ater	At end of break-in			At tailure										
no	BBI	ABI	AT	δ,/δ, μιπ	δ ₁ , μιπ	ь _{а.} µіп,	հ _m . µւո	۸m	δſ. µin	h _m . μιη	۱mf*	11	Pr ib	we. ppi	163	I., 	1		Modi	
897 898 899 8100 8101	0.5/2 0 0.5/1.0 0 0/1.2 1.5/1.5 0 0/1 0	1.0/1.5 1.0/1.3 0.5/1.0 1.8/1.5 0 5/1 0	1.8/2.0 1 5/1 8 1.0/1.5 1.5/1.5 1 0/1.5	13.7/12.3 10 3/10.7 12.7/10.2 12.1/10.5 10.2/13.3	26 0 21.0 22.9 22.6 23 5	16.6 17.1 17.5 13 6 16 5	5 2 5.2 5.2 5.2 5.2 5.2 5.2 5 6	0 31 0 30 0 29 0 32 0 33	165 168 170 161 161	58 66 67 65 6.2	0 35 0 38 0 38 0 48 0 37	0 033 0 030 0 033 0 033 0 035 0 031	1,850 750 650 800 1 300	11,176 6,122 5,565 6,391 8,833	201 195 195 196 197	303 240 236 250 260	234 136 138 163 183	<pre><3- 375 374 413 443</pre>		
			Average	11.8/11 4	23.2	16.3	53	0 31	16 \$	6,4	0 39	0.032	1,070	7617	197	258	171	125		
			Std dev	1.5/1.3	18	1.5	02	0 01	0.4	04	0.05	0 002	503	2 350	1	27	40	6.		
*Based	en 8 ₂ .									_							•	•		

REFERENCES

- 1. Carper, H. J., Anderson, E. L., and Ku, P. M., "Development of the AFAPL Disk Tester for Gear Lubrication Research," AFAPL Technical Report 71-63, August 1971.
- 2. Blok, H., "Les temperatures des surface dans des conditions de graissage sous pression extreme," Congr. mondial pétrole, 2 .ne Congr., Paris, Vol. 3, 1937.
- 3. Blok, H., "Theoretical Study of Temperature Rise at Surfaces of Actual Contact under Oiliness Lubricating Conditions," Proc. Gen. Disc. on Lubrication, I. Mech. E., 1937.
- 4. Dowson, D., "Elastohydrodynamic Lubrication," Interdisciplinary Approach to the Lubrication of Concentrated Contacts, P. M. Ku, editor, NASA SP-237, 1970.