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REPORT NO. 2309
THEORETICAL AND EXPERIMENTAL EVALUATION
OF TEST CELL "D"
EJECTOR SYSTEMS
(HOT FLOW PROGRAM)

AGC--2309

August 1962

MASTER

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ROCKET ENGINE OPERATIONS, NUCLEAR

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for

Aetron Division
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with

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ABSTRACT

An experimental and theoretical evaluation of two specific ejector systems was made to determine the applicability of the designs for use as exhaust disposal systems in Test Cell "D." The experimental tests were made with 1/11 scale models using cold and heated nitrogen to simulate the exhaust gases. Data on gas dynamics, heat transfer, film cooling effects, secondary ejector performance, purge characteristics, combined flow start up, and back flow problems, were obtained during this work. Design implications are drawn as a result of the experimental and analytical data.

Approved:

for W. D. Stinnett

FOREWORD

This report describes work done in fulfillment of Contract AT (29-2)1060, between June 1961 and December 1961 and is submitted in partial fulfillment of that contract which states in part:

provide the necessary personnel facilities and materials to design and fabricate a scale model exhaust system, and to test and interpret the data for two sizes of experimental exhaust systems for Test Cell "D."

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NOMENCLATURE AND SYMBOLOGY

NERVA	Nuclear Engine Rocket Vehicle Application
NTS	Nevada Test Site
Pheobus	A nuclear engine, with 3.6 times the power of NERVA
pull-in	When the shock system becomes stabilized in the ejector
start	A sudden lowering of the cell pressure to a minimum
A	= area
a	= ambient
c	= chamber
C_a^*	$= \frac{\rho_l V^{*2}}{p_b - P_p}$
C_h	= coefficient of convective heat transfer of film cooling
C_p	= specific heat at constant pressure
C_{pl}	= specific heat of film coolant in the liquid phase
C_{pv}	= specific heat of vapor
D	= diameter; for the duct, internal diameter
e	= nozzle exit; base of natural logarithms
F	= force or thrust
f	= friction factor
g	= gravitational constant
h	= heat-transfer coefficient
Δh_v	= heat of vaporization of liquid film coolant
J	= energy conversion factor
K	= a constant
L	= length; for the duct, length along centerline
ln	= natural logarithms

NOMENCLATURE AND SYMBOLOGY (cont.)

M	=	Mach No.
N_2	=	nitrogen content by volume
O_2	=	oxygen content by volume
P	=	static pressure
P_a	=	ambient pressure
P_c	=	chamber pressure
P_{18}	=	static pressure near exit
P_{T7}	=	total pressure, at exit, in center
Q	=	heat absorbed by liquid coolant
Q_o	=	heat transferred to surface of the liquid film
q_r	=	heat transferred per unit surface area of the liquid film by radiation
R	=	engineering gas constant
R_{eb}	=	Reynolds Number of bulk gases
R_e^*	=	Reynolds Number of coolant through injection ports
t	=	total or stagnation conditions; time
T	=	static temperature
T_a	=	Initial static temperature in cell at time $t = 0$
T_b	=	bulk or average temperature of combustion gases
t_i	=	supply temperature of liquid coolant
T_r	=	recovery temperature
T_v	=	saturation temperature of liquid coolant
T_u	=	temperature at surface of liquid film
v	=	environmental cell
V	=	volume
$\dot{V}$	=	volumetric flow rate

NOMENCLATURE AND SYMBOLOGY (cont.)

V^*	=	normal component of the critical velocity of coolant through injection ports
V_a	=	partial volume of V_2 occupied by air
V_b	=	velocity of bulk gases
V_{N_2}	=	partial volume of V_2 occupied by nitrogen
V_{O_2}	=	partial volume of V_2 occupied by oxygen
w	=	mass of oxygen in V_2 at time t
$\dot{w}$	=	mass flow rate, lb/sec

Superscripts

*	=	throat conditions; sonic conditions
-	=	mean value of mixed flow (such as $\bar{T}_t$, etc.)

Subscripts

1,2,3	=	stations along the ejector
a	=	ambient
b	=	backflow
c	=	chamber
D	=	duct
e	=	nozzle exit
F	=	separation
m	=	mean value
o	=	condition downstream of point
s	=	secondary ejector; secondary fluid
st	=	start
t	=	total
v	=	environmental cell pressure
V	=	environmental cell

NOMENCLATURE AND SYMBOLOGY (cont.)Greek Letters

β	=	efficiency factor $\approx 40\%$; nozzle leading edge angle
γ	=	ratio of specific heats
δ	=	plume boundary angle
θ	=	nozzle trailing edge angle
λ	=	secondary ejector nozzle wedge angle
μ_v	=	viscosity of coolant at saturation temperature
ρ_n	=	density of bulk gases
ρ_b^1	=	density of film coolant
ϕ	=	nozzle centerline angle
α	=	Prandtl-Meyer angle

SECTION I

SUMMARY - HOT-FLOW TESTS PERFORMED ON TEST CELL D EJECTOR SYSTEM

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I. SUMMARY OF HOT FLOW TEST PROGRAM PERFORMED ON THE TEST CELL D EJECTOR SYSTEM

Covered by this report are evaluation of two scale models of ejector configurations for Test Cell "D" through six basic types of tests. These tests included internal gas dynamics, heat transfer, film cooling, secondary ejector and start-up purge, and backflow; the results of which were used as basis for forecasts of full-scale systems performances.

These predictions indicated three major problem areas inherent to these specific designs.

A. The heat-transfer rate in the elbow region of the duct would be too high ($\sim 12 \text{ Btu/in}^2/\text{sec}$) for a successful design.

B. The secondary ejector systems will require more development effort.

C. Flow instabilities were noted in the steady-state region, and must be eliminated through design changes.

A more detailed description of the scale model tests is presented below in Table 1, which summarizes the tests performed on the two duct configuration.

TITLE	TEST NO.	RUN NOS.	CONFIGURATION	P _c	T _c	SECONDARY	P _{sc}	OTHER TEST CONDITIONS	REMARKS	
						EJECTOR CONFIGURATION			*S.T. = Satisfactory Test *U.T. = Unsatisfactory Test	
Gas Dynamics	1	13	P	775	1600°R	--	---		S.T.	
		14	P	750	1600°R	--	---		S.T.	
		15	P	725	1600°R	--	---		S.T.	
		16	P	700	1600°R	--	---		S.T.	
		17	P	700	1600°R	--	---		S.T. Repeat of Run 16 (Temp. Low).	
Gas Dynamics	2	1	N	500	1600°R	--	---		*U.T. Chamber Temp. varied from 200-1000°F during run (checkout test)	
		2	N	900	1600°R	--	---		U.T. Chamber Temp. was too low (checkout test)	
		3	N	450-600	1600°R	--	---		U.T. Chamber temp. varied over a 50° range (checkout test)	
		4	N	450-700	1600°R	--	---		U.T. P _v pulled down late - possible cell leak (checkout test)	
		5	N	950	1600°R	--	---		S.T. Infrared camera did not work (checkout test)	
		6	N	950	1600°R	--	---		S.T. Infrared camera worked satisfactorily (checkout test)	
		7	N	450	1600°R	--	---		S.T. Elbow calorimeter and T _c no good (checkout tests)	
		8	N	400	1600°R	--	---		S.T. T ₆ no good	
		10	N	450	1600°R	--	---		S.T. Nozzle flowed full but cell didn't pull down	
		11	N	540	1600°R	--	---		S.T. Cell pulled down	
		12	N	500	1600°R	--	---		S.T.	
		90	N	300	1600°R	--	---		S.T. Ran for more duct pressure data	
		91	N	440	1600°R	--	---		S.T. Repeated for more duct pressure data	
	92	N	550	1600°R	--	---		S.T. Repeated for more duct pressure data		
Heat Transfer	3	18	P	1100	1600°R	--	---		S.T.	
		20	P	1100	1600°R	--	---		S.T.	
Heat Transfer	4	19	P	1100	1600°R	--	---	1° Nozzle Gimbal	S.T.	
Heat Transfer	5a	25	N	550	1600°R	--	---		S.T.	
		112	N	550	1600°R	--	---		S.T. Burst diaphragm used to obtain more data points in elbow region	
		113	N	550	1600°R	--	---		S.T. Repeat of run 113 to check reproducibility	
	5b	26	N	715	1600°R	--	---		S.T.	
	5c	9	N	900	1600°R	--	---		S.T.	
		24	N	916	1600°R	--	---		S.T.	
	5d	27	N	500	1600°R	--	---		S.T.	
Heat Transfer	6a	21	N	500	1600°R	--	---	1° Nozzle Gimbal	S.T.	
		6b	22	N	916	1600°R	--	---		S.T.
		23	N	916	1600°R	--	---		S.T. Repeat of Run 22	
Film Cooling	7	71	N	916	1600°R	--	---		U.T. Potter & Orifice plugged	
		72	N	916	1600°R	--	---		S.T.	
		73	N	916	1600°R	--	---		S.T.	
		74	N	916	1600°R	--	---		S.T.	
		75	N	916	1600°R	--	---		S.T.	
		76	N	916	1600°R	--	---		S.T.	
		77	N	916	1600°R	--	---		S.T. No water coolant flow required	
Secondary Ejector	8a	28	N	---	---	A	150		S.T. checkout test	
		29	N	---	---	A	200		S.T. checkout test	
		30	N	---	---	A	250		S.T. checkout test	
		31	N	---	---	A	275		S.T. checkout test	

Table 1
Test Summary

TITLE	TEST NO.	RUN NOS.	CONFIGURATION	P _c	T _c	SECONDARY EJECTOR CONFIGURATION	P _{sc}	OTHER TEST CONDITIONS	REMARKS
Secondary Ejector	8a	32	N	---	---	A	400		S.T. Checkout Test
		33	N	--	---	A	215		S.T. Checkout Test
		34	N	--	---	A	175		S.T. Checkout Test
		35	N	--	---	A	50		S.T. Checkout Test
		36	N	--	---	A	75		S.T. Checkout Test
		37	N	--	---	A	100		S.T. Checkout Test
		38	N	--	---	A	125		S.T. Checkout Test
		39	N	--	---	A	100-400		S.T. Checkout Test
		40	N	--	---	A	100-400		S.T. Point of lowest P _v (3.96) recorded during this run at a P _c of 230 psia
		41	N	--	---	A	50		S.T. Checkout Test
		42	N	--	---	A	75		S.T. Checkout Test
		43	N	--	---	A	100		S.T. Checkout Test
		44	N	--	---	A	125		S.T. Checkout Test
		45	N	--	---	A	150		S.T. Checkout Test
		46	N	--	---	A	175		S.T. Checkout Test
		47	N	--	---	A	200		S.T. Checkout Test
		48	N	--	---	A	225		S.T. Checkout Test
		49	N	--	---	A	250		S.T. Checkout Test
		50	N	--	---	A	280		S.T. Checkout Test
		51	N	50	Cold	A	175		S.T. Runs 51-60 introduce engine
		52	N	150	Cold	A	175		S.T. flow to see if P _v could be
		53	N	250	Cold	A	175		S.T. reduced
		54	N	350	Cold	A	175		S.T.
		55	N	550-600	Cold	A	175		S.T.
		56	N	50	Cold	A	125		S.T.
		57	N	150	Cold	A	125		S.T.
		58	N	250	Cold	A	125		S.T.
		59	N	350	Cold	A	125		S.T.
		60	N	550	Cold	A	125		S.T.
Secondary Ejector	8b	63	N	--	--	B	50		S.T.
		64	N	--	--	B	100		S.T.
		65	N	--	--	B	150		S.T.
		66	N	--	--	B	125		S.T.
		67	N	--	--	B	175		S.T.
		68	N	--	--	B	190		S.T.
		69	N	--	--	B	250		S.T. Point of lowest P _v = 4.9
		70	N	--	--	B	300		S.T.
		78	N	--	--	C	50		S.T.
		79	N	--	--	C	100		S.T.
Secondary Ejector	8c	80	N	--	--	C	150		S.T.
		81	N	--	--	C	200		S.T.
		82	N	--	--	C	250		S.T. Point of lowest P _v = 4.062
		83	N	--	--	C	300		S.T.
		84	N	--	--	C	300		S.T. Removal of upstream portion of duct to observe flow.
		85	N	--	--	C	300		S.T. Plexiglass cover placed upstream of S.E. to observe flow
		86	N	--	--	C	300		S.T.
		87	N	--	--	C	250		S.T.
		93	N	--	--	D	50		S.T.
		94	N	--	--	D	65		S.T.
Secondary Ejector	8d	95	N	--	--	D	75		S.T.
		96	N	--	--	D	115		S.T.
		97	N	--	--	D	135		S.T. Point of lowest P _v = 1.96
		98	N	--	--	D	160		S.T.
		99	N	--	--	D	175		S.T.
		100	N	--	--	D	125		S.T.
		101	N	--	--	D	132		S.T.
		102	N	--	--	D	140		S.T.
		103	N	--	--	D	135		S.T.
		104	N	--	--	D	145		S.T.
		105	N	--	--	D	150		S.T.
		106	N	--	--	D	156		S.T.
		107	N	--	--	D	134		S.T.

Table 1 (Cont.)
Test Summary

TITLE	TEST NO.	RUN NOS.	CONFIGURATION	P _c	T _c	SECONDARY EJECTOR CONFIGURATION	P _{sc}	OTHER TEST CONDITIONS	REMARKS
Secondary Ejector	8e	114	N	--	--	Da	75		S.T.
		115	N	--	--	Da	100		S.T.
		116	N	--	--	Da	125		S.T.
		117	N	--	--	Da	150		S.T.
		118	N	--	--	Da	175		S.T.
		119	N	--	--	Da	215		S.T.
		120	N	--	--	Da	240		S.T.
		121	N	--	--	Da	195		S.T.
		122	N	--	--	Da	200		S.T.
		123	N	--	--	Db	50		S.T.
Secondary Ejector	8f	124	N	--	--	Db	70		S.T.
		125	N	--	--	Db	80		S.T.
		126	N	--	--	Db	115		S.T.
		127	N	--	--	Db	130		S.T.
		128	N	--	--	Db	140		S.T.
		129	N	--	--	Db	160		S.T.
		130	N	--	--	Db	180		S.T.
		131	N	--	--	E	40		S.T.
		132	N	--	--	E	70		S.T.
		133	N	--	--	E	100		S.T.
Secondary Ejector	8g	134	N	--	--	E	140		S.T.
		135	N	--	--	E	160		S.T.
		136	N	--	--	E	180		S.T.
		137	N	--	--	E	210		S.T.
		138	N	--	--	E	180		S.T. Point of lowest P _v = .636
Purge	9	88	N	--	--	C	300		S.T.
		89	N	--	--	C	300		S.T.
Startup	10a	108	N	50-700	Cold	D	162		S.T.
	10b	109	N	50-700	Cold	D	50		S.T.
	10c	110	N	50-700	Cold	D	114		S.T.
Backflow	11a	61	N	100-0	Cold	--	---		S.T. No backflow observed
	11a	62	N	100-0	Cold	--	---		S.T. No backflow observed
	11b	139	N	550	Cold	--	---		S.T. No backflow observed
	11c	140	N	916	Cold	--	---		S.T. No backflow observed

Table 1 (Cont.)
Test Summary

A. INTERNAL GAS DYNAMICS

Since the configurations of the NERVA and Phoebus ejectors were selected on the basis of cold-flow tests, the effect of heated nitrogen on the performance of the ejector and ejector operating characteristics needed to be considered.

Cold-flow tests⁽¹⁾ indicated that the deflector-duct configuration was acceptable, and that "pull-in" of the duct (start of a stable shock system) occurred simultaneously with "pull-in" of the environmental cell (minimum cell pressure), which occurred at a chamber pressure of 38 atm (475 psia at NTS). However, cold-flow tests of a constant area duct indicated that the two conditions would not necessarily occur simultaneously.

Hot-gas flow tests of the deflector duct design differed from the cold-flow tests, in the respect that "pull-in" of the duct did occur at a higher chamber pressure than was required for the environmental cell. In addition, the hot-gas test data indicated that the "pull-in" points may not be reproducible to the extent shown on the figures. During one test, pressure measurements indicated a potential instability at a chamber pressure of 42 atm, which is above the "pull-in" pressure.

⁽¹⁾ B. Mandell, G. O. Patmor, Cold-Flow Testing, Scale-Model Program for Test Cell "D" Exhaust System, Aerojet-General Report 2129, November 1961.

B. HEAT TRANSFER

The heat-transfer portion of the hot-flow test program was established for the purpose of determining the value of the gas-film heat-transfer coefficient for the Test Cell "D" scale models, and for correlating it in such a way as to make possible a reasonably accurate calculation of the heat flux in the full-scale system.

The results of the tests indicate that, for the existing design, heat-transfer rates along the bottom center line of the elbow will reach a maximum of 12 Btu/in²-sec when a NERVA engine is utilized. This heating rate was found to occur at the 45° point in the bend, at the point where the gas impingement is most pronounced; it is a larger heat-transfer rate than an aluminum duct can withstand, and is too large a rate to permit coatings to be used with any assurance of success.

C. FILM COOLING

The shape of the ejector and location of the shocking process of the gas flow in the ejector indicate that there would be a region of high heat flux at the outer portion of the 90° bend. Film cooling of a portion of the ejector was investigated qualitatively as a means of reduction of heat flux in hot areas. The results indicated a coverage of the first 45° of the 90° bend on the outside of the bend, which was subjected to the highest heat fluxes and coverage of the full 90° on the inside of the bend. Applying the one-dimensional theory to the full scale test cell "D" ejector for the NERVA engine, it was determined that the maximum length that can be film cooled with water at 100% efficiency is 3 inches. The test results are qualitatively in agreement with the one-dimensional theory, but a much more exact analysis is necessary to determine the magnitude of the scaling factors involved, the method of injection, and the actual surface area covered.

D. SECONDARY EJECTOR AND STARTUP

Simulation of starting the NERVA engine at altitude, and the requirement of explosion-free operation, both necessitate use of a secondary ejector for the duct, for the purpose of maintaining altitude conditions in the environmental cell at all times during the startup and operation of the engine. Tests made with secondary ejectors in the deflector duct model have thus far not accomplished this objective.

For zero gas flow from the NERVA engine, the scale-model test results show that the desired simulation of 60,000 ft altitude will not be realized until the secondary ejector nozzle centerline is at an angle of, or less than, 12.5° . The most satisfactory performance came when the secondary ejector nozzle centerline was parallel to the centerline of the duct. This configuration required an increase in duct cross sectional area downstream of the secondary ejector nozzle.

Once the NERVA engine had been started, the ejector configuration, in conjunction with the gas dynamics of the system, caused an increase in the cell pressure to a point where the simulation of 60,000 ft was no longer realized.

No tests have been made for the increased area duct with primary flow, but it is expected that conditions would be improved. These test results indicate that the desired operational point for the secondary ejector has not been established, and that considerable improvement can be obtained by further tests.

E. PURGE

Before the nuclear engine could be started, the oxygen content in the environmental cell and the ejector had to be reduced to a safe limit to prevent a hydrogen-air explosion. A literature search indicated that, to be safe, the oxygen content should be reduced to some value below 4% by volume before the engine was started.

Two theoretical models were analyzed to determine the amount of purge gas needed to reduce the oxygen content in the cell. One model assumed 100% perfect mixing with the existing gas in the cell, and the other assumed slug flow with no diffusion of the gases at the interface. Since it is not possible to have perfect mixing or slug flow without diffusion, tests were conducted and the results compared to the theoretical results.

The maximum purge gas needed to reduce the test cell "D" oxygen content to 1% by volume is 110 lb, if the environmental pressure is 1 psia, and temperatures of the purge gas and air are 540°R.

F. BACKFLOW ANALYSIS

The use of hydrogen as a propellant for nuclear engines requires that air be excluded from the ejector system at all times during engine operation.

There are three times when air backflow might exist:

during the long period of engine shutdown when there are low chamber pressures, at the point where the engine chamber pressure is just insufficient to keep the start condition, and

when supersonic flow exists at the duct exit.

The results of the first 2 test conditions indicate that there is no backflow of air in the boundary layer at the ejector exit. Acceptable data could not be obtained for the third condition due to the highly turbulent nature of the boundary layer; such data as was obtained for this condition was secured in a Flow Visualization Program⁽²⁾ by inserting tufts a short distance in from the ejector exit and photographing their behavior. These photographs indicate that again there was no backflow of air into the ejector.

⁽²⁾ W. R. Thompson, C. W. Heinz, Duct Flow-Visualization Studies Scale-Model Program for Test Cell "D" Exhaust System Aerojet-General Report 2220, August 1962.

SECTION II

INTRODUCTION

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II. INTRODUCTION

Test Cell "D" will be used to static test nuclear-rocket engines to be used as upper stages of multistage space vehicles. Because the exhaust gas from a nuclear rocket is highly flammable hydrogen, the open type of flame deflector, which is utilized for testing chemically-fueled rockets, can not be used. The hydrogen exhaust must be ducted out of contact with air to a sufficient distance from the facility so that the hydrogen can be disposed of safely by burning. Otherwise, the test facility could be enveloped in flaming hydrogen, or be overheated by radiation from the hydrogen-air flame. The system must accomplish this disposal while excluding air from contact with the exhaust hydrogen at all points up to the burning area, because if air mixes with the hot hydrogen inside the ejector system, an explosion could occur. Such an explosion could not only damage the ejector system, the nuclear test engine, and the test stand, but also could scatter sufficient radioactive particles to render the test stand unapproachable for an extended period, with a consequent delay in the engine development program.

For upper-stage vehicles, rocket engines with large-area-ratio exhaust nozzles are desirable to optimize performance. To test such engines at sea level under conditions that permit full expansion of the flow in the exhaust nozzle, it is necessary to provide some means of reducing the pressure in the duct into which the flow from the exhaust nozzle discharges. If properly designed, the ejector selected for this purpose can use the energy of the rocket exhaust to establish and maintain a pressure rise in the ejector, thus providing a reduced pressure around the exit of the exhaust nozzle.

The Test Cell "D" exhaust system is required to perform under conditions which impose more severe heat-transfer conditions than those imposed on chemical-rocket nozzle-exhaust systems. Heating rates are higher because hydrogen is an excellent heat-transfer fluid. The exhaust length dictated by the requirements for hydrogen disposal exceeds the length used on chemical-rocket nozzles.

Since heating rates are comparatively low in chemical-rocket systems, and cooling water costs are modest, therefore, for chemical rockets, the cooling system can be designed to use excess water to compensate for the uncertainty involved in the prediction of the cooling requirements. However, this situation does not apply to the nuclear-engine ejector, as the quantity of water used and the pressure required would add substantially to the installation costs. At the same time, safe operation must be assured.

To assure a method for safe operation of the ejector system, a scale-model test program was conducted to investigate problem areas of the ejector operation. The test program conducted is outlined in Table 2.

TEST PROGRAM

Test No.	Title	Engine Chamber Pressure	Engine Chamber Temp.	Ejector System	No. of Tests	Test
1	Gas Dynamics	500-800;1100	1600°R	Phoebus	5	Determine effects of heated nitrogen upon starting pressure and internal gas dynamics.
2	Gas Dynamics	400-550;900	1600°R	NERVA	15	Same as Test 1
3	Heat Transfer	1100	1600°R	Phoebus	2	Determine precise quantitative values of transient and steady state wall temperatures.
4	Heat Transfer	1100	1600°R	Phoebus	1	Same as Test No. 3 with 1 degree of nozzle gimbaling.
5a	Heat Transfer	550	1600°R	NERVA	3	Same as Test 3
5b	Heat Transfer	730	1600°R	NERVA	1	Same as Test 3
5c	Heat Transfer	900	1600°R	NERVA	2	Same as Test 3
5d	Heat Transfer	500	1600°R	NERVA	1	Same as Test 3
6a	Heat Transfer	550	1600°R	NERVA	1	Same as Test No. 5a with 1 degree of nozzle gimbaling.
6b	Heat Transfer	900	1600°R	NERVA	2	Same as Test No. 5c with 1 degree of nozzle gimbaling.
7	Film Cooling	900	1600°R	NERVA	7	Determine the coverage and wall temperatures for various film coolant flow rates.
Test No.	Title	Engine Chamber Pressure	Secondary Ejector Chamber Pressure	No. of Tests	Secondary Ejector Model	Test
8a	Secondary Ejector	0 psig	50-250	33	A	Determine performance of various secondary ejector nozzle configurations. See Figure VII-7
8b	Secondary Ejector	0 psig	50-250	8	B	
8c	Secondary Ejector	0 psig	50-250	10	C	
8d	Secondary Ejector	0 psig	50-250	15	D	
8e	Secondary Ejector	0 psig	50-250	9	D _A	Vary the centerline L/D for secondary ejector #D and determine performance. See Figure VII-7
8f	Secondary Ejector	0 psig	50-250	8	D _B	
8g	Secondary Ejector	0 psig	50-250	8	E	
9	Purge	0 psig	150	2	C	Determine oxygen content of cell after introduction of purge gas. See Figure VIII-2
10a	Startup	50-700	325	1	D	Find resulting ejector system performance when both engine and secondary ejector are in operation.
10b	Startup	50-700	75	1	D	
10c	Startup	50-700	125	1		
11a	Backflow	100-0 psig	0	2	-	Determine pressure and exhaust gas characteristics at and near the duct exit at various conditions.
11b	Backflow	550-400	0	1	-	
11c	Backflow	900	0	1	-	

* Tests 8-11 were made with NERVA Ejector System and using cold nitrogen.

Table 2
Test Program

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SECTION III

GENERAL APPARATUS

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III. GENERAL APPARATUS

High-pressure nitrogen was passed through two scale-model nozzles and their respective ejectors, after pre-heating 1100°F in a stored energy heater (Figures 1 and 2), for the heated-gas tests. An altitude chamber or airtight environmental cell (Figures 3, 4 and 5) enclosed each nozzle during testing. The cell had an internal volume of 1.22 cubic feet.

The dimensionally scaled down versions of the NERVA and Phoebus nozzles were "fired" into their respective ejectors (Figures 6 and 7) which has been selected as a result of previous cold-flow tests⁽⁴⁾. The converging sections of these nozzles had half-angles of 45° , and the diverging portions of the nozzles were contoured (Figure 8). Both nozzles had an expansion ratio of 40:1. Nozzle calibration curves, as shown in Figures 9 and 10, were used to calculate the gas-flow rate from chamber pressure and temperature measurements.

Figure 1 provides an overall view of the test bay, while Figure 11 shows the parallel manifolding of the high-pressure nitrogen from the storage bottles to a single line leading to the pressure-regulation setup. The flow split, before going through two dome-loaded, pressure regulators (Figure 12), and then returned, by a single line leading to the heater (Figures 1 and 2).

Figure 4 shows the external components of the NERVA ejector system, and their installation in the test bay; including the cylindrical section, the conical section, the secondary ejector, and the duct, as defined in Figure 3, and the two inlet manifolds of the secondary ejector. The feed line from the heater entered the insulated environmental cell as shown in the top right of that Figure. The feed line connected with the nozzle inside the environmental cell, (Figure 5), which is a photo looking upward into the environmental cell with the bottom cover plate removed. The nitrogen expanded through the nozzle and entered the ejector. The

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⁽⁴⁾ B. Mandell, G. O. Patmor, Cold-Flow Testing, Scale Model Program for Test Cell "D" Exhaust System, Aerojet-General Report 2129, November 1961.

exterior of the ejector was insulated with 1/8-in. asbestos and 1-in of glass wool during all heat transfer tests, which was removed for the photographs.

Pressure measurements were taken using Pace and Wiancko pressure transducers. These transducers were mounted on a vertical panel as shown in Figure 13. Most of the transducers were referenced to a vacuum through use of a vacuum pump (shown in the lower left of Figure 13), and to a vacuum manifold extending to each transducer. The transducers for nozzle-chamber pressure, secondary-ejector chamber pressure, pressure at 45° point of elbow, and pressure in the purge line, were all referenced to atmosphere. Ambient pressure (P_a) measurements were made with a mercury manometer and were corrected for ambient temperature, which varied between 70 and 85° during the test program, but was constant during each test. The confidence range for 95% of the chamber pressure data is ± 13 psi, and for the nozzle exit and cell pressure data is ± 0.022 psi.

Pressure measurements were recorded by two methods. During the course of an individual test, key pressures were observed and recorded by an oscillograph. The remaining pressure measurements were sampled by a 100-channel electronic sampling switch and recorded on IBM tape. This tape, containing the pressure history of an ejector system "firing" was processed by an IBM 704 computer, with outputs typed by computer on output sheets.

Temperature measurements were taken with Chromel-Alumel thermocouples welded directly to the outside of the ejector wall (Figure 14). The voltages induced within the thermocouples were sensed by the sampling switch and each thermocouple voltage was transferred to magnetic IBM tape. The IBM tape was processed by an IBM 704 computer, and the typed output sheet provided a temperature history at each thermocouple location.

Photo
1161-226

Figure 1
Stored Energy Heater and Overall View
of Test Bay (Front View)

Photo
1161-227

Figure 2
Stored Energy Heater
(Rear View)

Figure 3
Ejector System

Photo
1161-225

Figure 4
NERVA Ejector System, Showing
Secondary Ejector Nozzle and Instrumentation

Photo
561-1080

Figure 5
Environmental Cell
Interior View

NERVA EJECTOR

Notes:

1. All diameters are internal
2. All dimensions are in duct diameters
3. For use with duct to nozzle-throat-area ratios of
4. Overall L/D along the center line
5. Model duct dia.

NERVA	PHOEBUS
35	56.4
17.28	9.81
4.76 in.	4.76 in.

PHOEBUS EJECTOR

Figure 6 (Top)
NERVA Ejector

Figure 7 (Bottom)
Phoebus Ejector

CONTOUR OF NOZZLES

Figure 8
Contour of Nozzles

Figure 9
Flow Calibration
NERVA Scale-Model Nozzle

Figure 10
Flow Calibration,
Phoebus Scale-Model Nozzle

Photo
561-1074

Figure 11
Nitrogen Storage Bottles

Photo
561-1076

Figure 12
Pressure Regulation Setup
with Dual Regulators

Photo
1161-228

Figure 13
Pressure Transducer Panel
with Vacuum Manifolds

Figure 14
Ejector Elbow
with "Weld on" Thermocouples

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SECTION IV

INTERNAL GAS DYNAMICS

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IV. INTERNAL GAS DYNAMICS

A. GENERAL DISCUSSION

The ejector configurations for the NERVA and Phoebus engines were selected on the basis of cold-flow tests⁽⁴⁾, so that it was desirable to obtain supplementary data on the ejector performance during the hot-gas testing program.

This information could then be compared with the results of the cold-gas tests as a check on the validity of the conclusions which resulted in the selection of the present duct configurations.

In addition to the steady-state pressure profiles in the duct system, the behavior during transients was of interest to define potential unstable regions of operation which could not be scaled.

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(4) B. Mandell, G. O. Patmor, Cold-Flow Testing, Scale-Model Program for Test Cell "D" Exhaust System, Aerojet-General Report 2129, November 1961.

B. TEST METHOD

At certain chamber pressures, the fluid leaving a nozzle is capable of evacuating the environmental cell, which in turn allows the nozzle to flow full. This sudden lowering of the environmental cell pressure (P_v), is termed a start, and the associated nozzle chamber and starting chamber pressure, is termed a P_c start. The effects of chamber pressure (P_c) on P_v and nozzle exit pressure (P_e) are termed "performance."

The starting of an ejector occurs when P_c start forces a Prandtl-Meyer expansion from the nozzle exit to the ejector wall. Chamber pressures in excess of P_c start then sustain low P_v and P_e (P_v/P_c and P_e/P_c are constant). Ejector "pull-in" occurs when the shock system in the ejector becomes stabilized, and happens during or after the start of the ejector.

Heated nitrogen at 1600°R was passed through both Phoebus and NERVA model nozzles and ejectors, and the nozzle-chamber pressures were varied from 700 to 1150 psia for the Phoebus engine, and from 300 to 950 psia for the NERVA engine. Ejector system pressure measurements were made with the pressure pickups located as shown in Figures 15 and 16.

Pressure Tap Locations
Figure 15 (top)
NERVA Ejector Model

Figure 16 (bottom)
Phoebus Ejector Model

C. TEST RESULTS

1. Performance

Figures 17 and 18 compare the performance of the hot- and cold-flow tests of the ejectors. The NERVA ejector started at a lower P_c/P_a for the hot-flow tests. The point of hot-flow P_c/P_a start was not located for the Phoebus ejector but probably occurred at a lower P_c/P_a than in cold-flow testing. Nevertheless, Figure 18 indicates that the Phoebus ejector began operating at a P_c/P_a which was less than the design P_c/P_a of the Phoebus engine, and the desired environmental conditions were provided. Figure 17 indicates that the NERVA ejector had a value of 31 for hot-flow P_c/P_a start and a value of 37.5 for cold-flow, or a reduction of 17%.

It is thus obvious that a hot working fluid is able to follow an isentropic expansion more closely (less increase in entropy) than a cold working fluid at these high area ratios. In addition, a heated working fluid has the same nozzle exit momentum (as compared to a cold working fluid) for lower values of chamber pressures, and therefore there is a resultant lower P_c/P_a start.

Performance Comparison-Hot & Cold Flow Tests
Cell & Nozzle Exit Pressure vs Chamber Pressure

Figure 17 (top)
NERVA Ejector Model

Figure 18 (bottom)
Phoebus Ejector Model

2. Ejector Operating Characteristics

Ejector system pressures as a function of P_c/P_a are shown in Figures 19 and 20 for the NERVA ejector and in Figure 21 for the Phoebus configuration. Shock attachment at the ejector wall accounts for the sudden changes in pressure which occurred when the ejectors started. Sudden increases in pressure were measured by transducers P_{7T} (total pressure at the exit), and P_5 (wall pressure at the outside 45° point of the elbow). These transducers, because of the strong dynamic component, sensed overall increases in pressure. When "start" occurred static pressure decreased sharply (as sensed by transducer P_4 at the entrance to the elbow) due to the sudden increase in Mach number throughout the ejector, this is illustrated in Figure 22, which is a typical oscillograph record of a "start."

The results of the cold-flow testing of the NERVA configuration indicated that this ejector was acceptable and that "pull-in" of the ejector occurred simultaneously with starting of the ejector (or "pull-in" of the environmental cell). This "pull-in" occurred at a chamber pressure of 37.5 atm (478 psia at NTS). Cold-flow tests of the Phoebus ejector indicated that the two conditions would not necessarily occur simultaneously.

Hot-gas flow tests of the NERVA ejector model differed from the cold flow tests since "pull-in" of the ejector did occur at a higher chamber pressure than was required for the environmental cell. Compare Figures 19 and 20. In addition, the hot-gas test data indicated that the "pull-in" points may not be reproducible to the extent shown on the figures. During one test, pressure measurements indicated a potential instability at 42 atm chamber pressure, which is above the nominal "pull-in" point for the ejector.

Ejector System Pressure vs Chamber Pressure
(Pressures are in Atmospheres)

$A_D / A^* = 35$

Gimbal $\alpha = 0^\circ$

Figure 19 (top)
NERVA Ejector Model
Cold Flow

Figure 20 (bottom)
NERVA Ejector Model
Hot Flow

Ejector System Pressures vs Chamber Pressure
(Pressures are in Atmospheres)

$A_D/A^* = 56.4$ Gimbal $\angle = 0^\circ$

Figure 21
Phoebus Ejector Model
Cold Flow

Figure 22
Oscillographic Record-NERVA Ejector
During the Starting Transient

3. Ejector Pressure Profile

Static wall pressures, as functions of L/D , are shown in Figures 23 and 24. The static pressure is increased to ambient (P_a) by a turbulent mixing process before the ejector has started ($P_c/P_a = 20.8$ & 27.7), as shown in Figure 21. For all chamber pressures sufficient to start the ejector, this rise in static pressure is first accomplished by a series of oblique shocks, and then by turbulent mixing.

Ejector Wall Static Pressure Ratio
vs L/D Along Centerline

Figure 23 (top)
NERVA Ejector Model
 $A_D/A^* = 35$

Figure 24 (bottom)
Phoebus Ejector Model
 $A_D/A^* = 56.4$, Gimbal $\angle = 0^\circ$

4. Effects of Cooling Working Fluid

A slight change in the pressure profile occurs when the gas is cooled, see Figures 25 and 26. While P_c and T_c were held relatively constant throughout, pressure readings were taken when the ejector walls were relatively cold (while heat was being transferred from the gas to the wall) and compared to pressure readings when the wall temperature was approximately that of the gas (no heat transfer occurring); this comparison showed a slight downstream shift of the shock pattern due to "cooling" of the gas. It is likely that the shock pattern in the full-scale ejectors will be slightly further downstream than indicated in these Figures (which are based on adiabatic conditions) because energy will be removed from the gas.

Note:
 $62.3 < \frac{P_C}{P_A} < 66.3$

— No Cooling of Working Fluid
 - - - Cooling of Working Fluid

Note:
 $75.1 < \frac{P_C}{P_A} < 77.2$

— No Cooling of Working Fluid
 - - - Cooling of Working Fluid

Effect of Cooling on Ejector Wall Static Pressure Ratio
 vs L/D Along Centerline

Figure 25 (top)
 NERVA Ejector Model

Figure 26 (bottom)
 Phoebus Ejector Model

D. CONCLUSIONS AND RECOMMENDATIONS

1. "Pull-in" of the ejector did occur at a higher chamber pressure ($P_c/P_a = 37$) than was required for the environmental cell ($P_c/P_a = 31$).
2. "Pull-in" of the ejector may not be reproducible to the extent shown on the figures.
3. During one test, pressure measurements indicated a potential instability at a chamber pressure of 42 atm. This condition of potential instability needs to be removed.
4. Hot-flow testing resulted in lower P_c/P_a start (31) in the ejectors than did cold-flow tests (37.5).
5. Cooling of the walls of the full-scale ejector will result in a slight downstream shift of the shock system.

SECTION V

HEAT TRANSFER

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V. HEAT TRANSFER

A. GENERAL DISCUSSION

As previously mentioned, the exhaust system for Test Cell "D" must be designed to vent hot gaseous hydrogen from a nuclear rocket engine to a sufficient distance from the test facility where the hydrogen can be disposed of safely by burning.

Aluminum alloy was chosen as the ejector system material primarily because of its desirable radioactive decay properties, but this material does not maintain its strength characteristics above temperatures of 400°F. The heat fluxes are much too high to permit effective cooling by radiation and free convection, so the ejector will be cooled by water. Normally, such cooling would present little difficulty; however, for Test Cell "D", the limited supply of water and the cost of water delivery to the ejector in terms of supply lines make the design of an efficient cooling system more critical. Therefore, the purpose of the hot-flow heat-transfer test program was to determine the value of the gas-film heat transfer coefficient for scale models of the duct, and to correlate these data in such a way as to make possible a reasonably accurate calculation of the heat flux in the full-scale system.

B. TEST METHOD

To evaluate local heat transfer conditions in the Test Cell "D" ejectors for operation with NERVA and Phoebus engines, a series of tests with heated nitrogen were made for each ejector. For the NERVA ejector (shown in Figure 29) 10 tests were made over the chamber pressure range from 550 to 965 psia, while for the Phoebus ejector (shown in Figure 30) 3 tests were made at 1100 psia. The specific test conditions are summarized in Table 2.

During these tests, measurements of local pressures and wall temperatures were made to provide the data necessary to evaluate the heat-transfer conditions in the ejector. The pressure and temperature measurement locations are shown in Figures 27 and 28 for the NERVA ejector, and in Figures 29 and 30 for the Phoebus ejector. The ejectors were both constructed of 5-in. Type 347 stainless steel tubing, 0.120-in. thick. Nitrogen gas was heated to approximately 1600°R by a stored capacity type heater, and injected into the test chamber at the desired chamber pressure, and chamber pressure and throat size governed the flow rate. Both the flow rate and the gas total temperature were held as nearly constant as possible throughout the test runs. Thirty thermocouples were spot welded on the outside of the ejector walls at points of interest, and the ejector was insulated with asbestos and glass wool to prevent heat losses. An automatic sampling switch monitored temperatures in millivolts at periodic intervals of 0.6, 0.8, or 1.0 seconds, dependent on the test, and recorded them on magnetic tape. The sampled data was then reduced and printed by the IBM 704 computer located at the Aerojet Azusa facility.

To evaluate the heat-transfer conditions in the Test Cell "D" ejector design, it was necessary to convert the measured wall temperatures to local heat-transfer coefficients. These values were converted to estimates of the heat-transfer conditions in the ejector by comparison with a simplified mathematical model of transient heating within the duct wall.

Figure 27 (top)
Pressure Tap Locations.

NERVA Ejector Model

Figure 28 (bottom)
Thermocouple Locations

Figure 29 (top)
Pressure Tap Locations

Phoebus Ejector Model

Figure 30 (bottom)
Thermocouple Locations

C. MATHEMATICAL MODEL

If the ejector wall is considered to be thermally thin (no temperature drop through the wall), with zero conduction of heat along the wall; if radiation of energy is neglected, and if the outside of the ejector is considered to be perfectly insulated, then the rate of rise of the wall temperature can be equated to the convective heat-transfer rate into the wall by the following energy balance:

$$\rho \ c \ b \ \frac{dT_w}{d\Theta} = h(T_r - T_w) \quad (1)$$

where

- ρ = density of the wall material
- c = specific heat of the wall material
- b = wall thickness
- T_w = wall temperature
- Θ = time
- h = heat-transfer film coefficient
- T_r = recovery-temperature of gases

Equation (1) can be used to relate the wall temperatures to heat-transfer conditions in several ways. The method used during this work was selected to minimize experimental errors through use of an integral method of comparing the test results with the mathematical model. Equation (1) was solved for wall temperature as a function of time for the case when the specific heat of the wall material and the heat-transfer coefficient were linear functions of the wall temperature, and the recovery temperature was a step function of time. This led to the differential equation

$$c_o (1 + B T_w) b \ \frac{dT_w}{d\Theta} = h_o (1 + A T_w)(T_r - T_w) \quad (2)$$

and solution

$$\ln \left[\frac{T_r - T_w}{T_r - T_i} \right] + \frac{1 - B/A}{1 + B T_r} \ln \left[\frac{1 + A T_i}{1 + A T_w} \right] = - \frac{h_o}{\rho c_o b} \frac{(1 + A T_r) \theta}{(1 + B T_r)} \quad (3)$$

where

T_i = initial ejector-wall temperature

c_o = specific heat of wall material extrapolated to zero^o

B = coefficient of specific heat variation with temperature

h_o = heat-transfer coefficient extrapolated to zero^o

A = coefficient of variation of heat-transfer coefficient with temperature

Examination of Equation (3) shows that the function of ejector-wall temperature on the left side of the equation is linearly related to time; a fact which allowed statistical methods to be used in interpreting the test data in terms of ejector heat-transfer conditions.

By using test data within the region of application of Equation (3), a least-squares regression curve of the following form was calculated for each data point

$$y = a_o + a_1 \theta \quad (4)$$

where

$$y = \ln \frac{T_r - T_w}{T_r - T_i} + \frac{1 - B/A}{1 + B T_r} \ln \frac{1 + A T_i}{1 + A T_w} \quad (5)$$

The slope, " a_1 ," was then related to the heat-transfer coefficient with the wall at the recovery temperature by the relation:

$$h_r = h_o (1 + A T_r) = \rho C_o b (1 + B T_r) a_1 \quad (6)$$

The recovery temperature, was estimated at each location in the ejector by constructing a fictitious shock system that would agree with the pressure measurements. The local Mach numbers were estimated (see Figure 31) and the recovery temperatures, as shown on Figure 32 were used.

Figure 31
Comparison of Approximate Mach Numbers
Calculated from Pressure Data and Those
Used in Reducing Heat-Transfer Data

Figure 32
 Recovery to Local Temperature Ratio
 vs
 Mach Number for Nitrogen and Hydrogen
 ($\gamma=1.4$)

The limits chosen for utilization of the test data were based on keeping the radiation effects below 10% of the convective heat-transfer rate, and introducing no more than a 10% error due to the assumption of a step variation in gas-recovery temperature. The steady-state temperatures of the duct wall were used to provide the radiation cut-off temperature by providing a combined estimate of the shape factor and effective emissivity at each location, while a solution to Equation (1), with an exponential variation in recovery temperature of the gas, provided an estimate of the initial data point time where Equation (3) applied.

Additional parameters were calculated from the test data, including the standard error of data fit, the standard error of the intercept, the slope of the regression line, and the correlation coefficient of the data with the regression curve. This process was repeated with $A = 0$, and with $B = 0$, to provide additional correlation data for later use. Table 3 shows a typical computer printout giving the pertinent parameters.

The coefficient A was determined by reducing the test data while varying A and selecting the value yielding a minimum % error in the slope. The coefficient B was determined by matching the published specific heat data⁽⁵⁾ for Type 347 stainless steel as shown in Figure 33.

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(5) Air Weapons Materials Application Handbook, Metals and Alloys, ARDC TR 59-66, December 1959.

PUN 211

P 7

RHO+C+B =0.0031750

INITIAL TEMPERATURE = 540.6550

RECOVERY TEMPERATURE = 1521.0000

26 VALUES FROM C 1.1100SEC, 541.9700R TO C 22.7100SEC, 768.3000R INCLUSIVE.

A	B	A0	A1	S.E.	S.E.A0	S.E.A1	P++2	H
-0.0000600	0.0003950	0.0104965	-0.00909	0.0009277	0.0003688	0.0000273	0.9997834	-0.0000462
-0.0000600	0.	0.0118609	-0.01153	0.0009295	0.0003695	0.0000273	0.9998650	-0.0000366
0.	0.0003950	0.0122260	-0.01218	0.0009948	0.0003955	0.0000293	0.9998616	-0.0000619

PUN 211

P 8

RHO+C+B =0.0031750

INITIAL TEMPERATURE = 546.5400

RECOVERY TEMPERATURE = 1518.0000

27 VALUES FROM C 1.1100SEC, 548.0550R TO C 23.5100SEC, 784.3950R INCLUSIVE.

A	B	A0	A1	S.E.	S.E.A0	S.E.A1	P++2	H
-0.0000600	0.0003950	0.0130963	-0.00942	0.0017474	0.0006621	0.0000467	0.9993332	-0.0000479
-0.0000600	0.	0.0149902	-0.01131	0.0016352	0.0006383	0.0000455	0.9996345	-0.0000378
0.	0.0003950	0.0154964	-0.01258	0.0016311	0.0006367	0.0000454	0.9996737	-0.0000639

Figure 33
Comparison of Specific Heat Values Used
and Experimental Values for S.S. 347

The discussion thus far has considered the case of a perfect ejector wall. Actually there are two main effects that must be corrected for interpreting the test data: conduction along the ejector wall, and temperature gradient through the wall.

To correct the test data for conduction effects, use can be made of the fact that very good correlations of the ejector wall temperatures with time were obtained from all three correlation methods. The last correlation method is equivalent to the solution for constant property values, which therefore can be used as a model for estimating the magnitude of conduction losses along the wall. Based on the assumption that the wall temperature at any location can be represented as an exponential function of time, this equation may be stated

$$T - T_i = (T_r - T_i) (1 - e^{-a_1(n)t}) \quad (7)$$

The equation governing two-dimensional conduction of heat in a thin wall is

$$k \frac{\partial^2 T}{\partial x^2} = \rho C_b \frac{\partial T}{\partial \theta} - h_{20} (T_r - T) \quad (8)$$

where

x = distance along the wall

h_{20} = heat-transfer coefficient, including 2-dimensional conduction effects

This equation can be approximated in finite difference form by

$$\frac{T_{(n+1)} - 2T_n + T_{(n-1)}}{\Delta x^2} = \rho \frac{cb}{k} \frac{dT}{d\theta} - h_{20} (T_r - T) \quad (9)$$

where

Δx is the distance between equally spaced thermocouples.

Substitution of Equation (7) into Equation (9) yields the result

$$\frac{h_{20}}{h_{10}} = 1 + \frac{kb}{\Delta x^2 h_{10}} \left\{ 2 - e^{[a_{1(n)} - a_{1(n+1)}]\theta} - [a_{1(n-1)} - a_{1(n)}]\theta \right\} \quad (10)$$

Equation (10) gives a good estimate of the amount that conduction effects have influenced the estimated heat-transfer coefficient, and is used as a multiplying factor on the computer printout results.

Derivation of the correction for temperature gradients through the wall is more involved, and hence only the results of Reference (6) will be presented.

The first eigenvalue governing conduction of heat into a homogenous material can be represented as:

$$\sqrt{\frac{a_1}{\alpha}} \ b \tan \sqrt{\frac{a_1}{\alpha}} \ b = \frac{hb}{k} \quad (11)$$

The slope " a_1 ," still has the same meaning as used in this work, but for the special case of constant heat-transfer coefficient and specific heat. As can be seen in Equation (11), interpretation of the slope is somewhat different. However, when the tangent of $\sqrt{\frac{a_1}{\alpha}} \ b$ is small enough to be represented by a single term in its power series expansion, Equation (11) reduces to the value used here. A multiplying factor was established from Equation (11) and used to correct the test data as follows:

$$\frac{h \text{ corr.}}{h \text{ uncorr.}} = \frac{\tan \sqrt{\frac{a_1}{\alpha}} \ b}{\sqrt{\frac{a_1}{\alpha}} \ b} \quad (12)$$

The other losses in the system were considered, and order of magnitude calculations were used to demonstrate that they were negligible. Both of the corrections made on the test data affected only those values near the peak heating locations. The maximum correction to any of the test data was 30%.

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(6) Design Criteria for Uncooled Flame Deflector, Aerojet-General Report 1286.

D. TEST RESULTS

Because of the sectional manifold system used in the Test Cell "D" duct design, the average and maximum heating rates in each section were desired. The portions of the duct cooled by each manifold section are shown in Figure 34, and will be used in describing the results obtained during the scale model program. An overall presentation of the test results is shown in Table 4, in which local heat-transfer coefficients are tabulated for each measurement location. The manifold section numbers are located in the right hand column. A comparable tabulation of the heat-transfer rates to the ETS-1 aluminum duct is presented in Table 5 and will be discussed later.

	Pc(psia)	550	900	920	925	625	590	
		RUN NUMBER						
TC No.	L/D	21	23	24	111	112	113	Full Scale Heat Flux Btu/in ² sec (T _w = 400°F)
		h _r x 10 ⁵ (Btu/in ² -sec OR)						
1	0.3							
2	1.1	4.73	7.66	6.2				0.4
3	1.1	4.73	7.5	6.7				0.4
4	2.3	4.14	--	6.0				0.4
5	2.3	--	--	4.18				0.3
6	3.0	4.13	6.8	6.6				0.4
7	4.2	4.62	8.5	8.4				0.5
8	5.4	4.79	7.7	7.0				0.4
9*	5.7	7.8	--	12.0				0.8
10	7.6	13.0	21.0	23.1	Not Measured			1.4
11	8.4	9.1	14.5	15.5				1.0
12	10.0	8.8	14.6	18.1				1.0
13	12.7	9.5	13.2	9.8				0.9
14	14.8	19.9	--	--				1.6
15	5.2	2.71	4.30	3.4				0.2
16*	7.0	3.23	5.1	4.13				0.2
17*	8.3	7.0	11.5	11.1				0.6
18	12.3	6.9	10.5	13.1				0.6
20	5.2	5.8	8.66	8.3	7.6	5.0	4.9	0.4
21*	5.8	15.4	23.3	21.1	21.7	14.1	13.5	1.2
22*	6.1	15.0	25.3	24.4	39.5	26.6	24.7	1.6
23*	6.4	--	--	--	52	35.2	31.0	2.5
+24*	6.8	110	200	230	200	--	144	12.0
25*	7.1	39.3	94	67	72	42.3	38.0	4.0
27*	7.9	13.5	31.7	22.1	26.8	17.3	--	1.5
28*	8.4	21.3	43.3	28.1	26.8	17.1	15.8	1.6
29	13.9	12.8	21.0	12.1	--	2.15	2.2	---
30	17.2				11.8	5.2	5.5	0.6
31*	5.6				9.0	5.8	5.4	0.5
32*	5.6				9.4	4.5	5.6	0.5
33	5.6				7.6	4.53	4.38	0.4
34*	5.9				19.9	14.0	12.4	1.0
35	5.9	Not Measured			19.9	14.0	12.4	1.0
36	6.3				8.6	5.6	5.4	0.5
37	6.4				63.7	48.3	39.8	3.4
+38*	6.7				--	170	146	12.0
39	6.7				13.5	6.9	10.1	0.8
40	7.1				26.2	18.3	15.9	1.2
41	7.2				54.2	11.9	13.9	2.0
42	7.4				50.3	--	--	2.7

* Corrected for wall thickness, conduction and Biot No.
+ max. h

Table 4
Heat-Transfer Coefficients for Nitrogen
in NERVA Model Ejector and Full-Scale
NERVA Ejector Heat-Transfer Rates

Figure 34
Arrangement of Sections
NERVA Ejector

D. ANALYSIS

The heat-transfer coefficients measured in the elbow area were quite high, and indicate a drastic change of the flow pattern in the elbow. Comparable data from open flame deflectors⁽⁷⁾ can be compared with this data in a qualitative manner to show trends.

In Reference⁽⁷⁾ it was shown that the heat-transfer rate from a fixed Mach number jet to a flame deflector reached a maximum in the area where the last jet streamline impinged on the deflector. Since the maximum mass flux on the deflector surface occurred in the same region for both the ETS-1 (Test Cell "D") duct and for an open-flame deflector, it would be expected that the local heating rates would follow the same trends. Experience with open-flame deflectors also indicated that the maximum heating rate to the deflector varied as the sine of the impingement angle of the last streamline, and did not depend on the flow conditions upstream of this point. Since, for this program, only one elbow radius (and hence impingement angle) was tested, it is not known whether the duct-heat-transfer will behave in this manner. The magnitude of the heat transfer to an open deflector was found to agree with the Colburn "j" equation⁽⁷⁾ when the pressure measurements were converted to a mass flux on the assumption of isentropic compression. It was realized that this is not at all representative of the physical process in a closed duct, so a heat-transfer calculation was made at the elbow after accounting for the loss in total pressure in the inlet section. This calculation gave reasonable agreement with the measured value (about 25% low), and lends credance to the use of open deflector trends to evaluate the happenings in the ejector. If the average flow conditions occurring in the ejector are used to evaluate the Reynolds Number, the test data obtained during this program also lies close to the predictions from the Colburn "j" equation, again substantiating the analogy.

⁽⁷⁾ Design Criteria for Uncooled Flame Deflector, Aerojet-General Report 1286.

F. CONCLUSIONS AND RECOMMENDATIONS

When the "j" equation is used as a scaling law, the conversion to the full-scale duct conditions becomes

$$\frac{Q/A_{fs}}{h_{sm}} = \frac{h_{fs}}{h_{sm}} (T_r - T_w)_{fs} \quad (13)$$

where

$$\frac{h_{fs}}{h_{sm}} = \left(\frac{\dot{w}_{fs}}{\dot{w}_{sm}} \right)^{.8} \left(\frac{D_{fs}}{D_{sm}} \right)^{1.8} \frac{\left(\frac{C_p K^{3/2}}{\mu} \right)_{fs}^{.4}}{\left(\frac{C_p K^{3/2}}{\mu} \right)_{sm}^{.4}} \frac{\left(T_c/T_f \right)_{fs}}{\left(T_c/T_f \right)_{sm}^{.8}} \quad (14)$$

When the test data from Table 4 for the ETS-1 duct was used with Equation (13) and (14), the heat-transfer rates, listed in the table as full-scale heat-transfer rates, resulted. Table 5 presents the equivalent results for the Phoebus engine configuration. It can be seen that, in both cases, the full-scale heat-transfer rate in the elbow region is considerable in excess of the maximum allowable with the present water-feed system.

Heat-transfer measurements made on scale models of the NERVA and Phoebus ducts for Test Cell "D" indicate that severe heat-transfer conditions exist in the elbow region of each ejector. Other regions of the ejectors will have heat-transfer rates that are acceptable for the existing design.

The heat-transfer conditions in the elbow region appear to follow the same trends as found for open-flame deflectors, although a quantitative correlation was not obtained. This relationship was used to estimate heat-transfer rates to the full-scale duct.

Based on experience with flame deflectors, the elbow heat-transfer conditions could be helped by a more gradual turning of the hot exhaust gases, and a reduction in maximum heat-transfer rate by a factor of 2 to 3 seems possible.

TC No.	P_c (psia) T_c ($^{\circ}R$) $h_r \times 10^5$ (Btu/in ² sec $^{\circ}R$)		Full Scale Heat Flux Btu/in ² sec($T_w=400^{\circ}F$)
	Test 19	Test 20	
1	11.2	9.7	0.8
2	6.2	8.1	0.6
3	4.5	4.9	0.4
4	5.2	5.4	0.4
5	4.0	--	0.3
7	3.8	4.3	0.3
8	4.2	5.1	0.3
9	22.6	--	1.8
10	14.7	10.3	1.0
11	12.9	11.4	1.0
15	2.6	--	0.2
16	13.0	14.3	1.1
17	8.8	9.6	0.7
18	12.9	13.7	1.1
19	3.5	4.0	0.3
20	9.3	--	0.7
21	25.6	29.7	2.0
22	60.8	100.1	6.5
23	85.5	114.0	8.0
24	45.7	118.0	9.5
25	20.0	17.9	1.5
26	16.2	18.2	1.4
30	12.6	13.6	1.1

Table 5
Heat-Transfer Coefficients for Nitrogen
in Phoebus Model Ejector and Full-Scale
Phoebus Ejector Heat-Transfer Rates

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SECTION VI

FILM COOLING

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VI. FILM COOLING

A. GENERAL DISCUSSION

The purpose of film cooling is to provide a thin layer of liquid on a surface exposed to high heat flux. This liquid film, when continuously supplied through injection holes, will keep the wall that it covers at a temperature at or below the boiling point of the liquid. Therefore, by utilizing the heat of vaporization of the liquid film, it is possible to provide cooling with much less liquid than if standard convective means are used, because of the much larger enthalpy change of the coolant.

B. TEST METHOD

To provide data on the efficiency of film cooling in the elbow region of the NERVA duct for Test Cell "D," a series of tests were made, which utilized distilled water as coolant. The water was introduced at the elbow inlet around the bottom half of the circumference, as shown in Figure 35.

Twenty-five equally spaced 0.076-in.-dia holes were drilled at a 45° angle of the outer half of the NERVA ejector, and a manifold was built and welded onto the ejector, as shown in Figure 36. Distilled water was regulated, measured, and introduced into the duct through the manifold and inlet holes.

Water was introduced into the ejector at the beginning of the 90° bend. The engine chamber pressure was held constant at 900 psia for the five tests made to provide the most severe condition anticipated. Different film coolant flow rates (0.088, 0.114, 0.145, 0.201 and 0.302 lb/sec.) were used for each test.

Steady-state temperature and pressure measurements were obtained from weld-on thermocouples and Pace pressure transducers. The location of the pressure pickups are shown in Figure 37, and of the thermocouples in Figure 38.

Figure 35
Ejector Film Cooling

Figure 36
Ejector Elbow
Showing Film Cooling Manifold

NERVA Ejector Model

Figure 37 (top)
Pressure Tap Locations

Figure 38 (bottom)
Thermocouple Locations

C. TEST RESULTS

The results of the film-cooling tests indicate a marked reduction in wall temperature in certain locations in the duct. Tabular results of the steady state wall temperature for various coolant flow rates, and for no-coolant flow, are shown in Table 6. The corresponding thermocouple locations are shown in Figure 38.

These results indicate a coverage of the first 45° of the 90° bend on the outside of the bend, which was subjected to the highest heat fluxes, and coverage of the full 90° on the inside of the bend.

Because of the lower static pressure at the inside of the 90° bend, the coolant water, even though not being injected on that half of the bend, was drawn to the inside and therefore cooled the upper portion of the bend.

Visual observation of the duct, after the film cooling tests, indicated that the coolant terminated at the location of the severest shock in the 90° bend (near Thermocouple 24). This removal of the coolant leaves the wall exposed near the region of highest heat flux. Based on the wall temperature measurements, the average length film cooled at the highest coolant flow rate (0.3 lb/sec) was 11 in.

During the film-cooling tests, it was noted that the heat-transfer coefficients in the conical portion of the ejector were increased by a significant amount. This is shown below, in a comparison of values of heat-transfer coefficients for Thermocouples 2, 3, 4, and 6 during a film-cooling test, to values for the same thermocouples during a standard test without film cooling.

<u>Thermocouple</u>	<u>Heat-Transfer Coefficients Btu/hr ft² °F</u>	
	<u>With Film Cooling</u>	<u>Without Film Cooling</u>
2	274	35
3	95	31
4	137	35
6	129	37

This increase in magnitude of the heat-transfer coefficient in the upper portion of the ejector leads to the conclusion that the shock pattern, when film cooling is employed, has shifted upstream. This prevents the use of data obtained in a smooth dry duct for prediction of maximum heat fluxes and critical areas in a duct which employs film cooling.

Thermo- couple #	COOLANT FLOW RATE					
	.302 lb/sec	.201 lb/sec	.145 lb/sec	.114 lb/sec	.088 lb/sec	0 lb/sec
DUCT WALL TEMPERATURE °R						
31	532	538	547	543	546	930
21	557	557	564	566	572	1095
22	574	581	587	588	595	1298
38	646	857	1123	1210	1291	1377
25	1306	1377	1404	1408	1439	1439
33	532	539	550	545	553	757
36	543	551	552	553	553	705
9	541	547	553	554	554	789
39	593	580	577	571	580	1128
40	985	1143	1201	1213	1244	1293
11	1089	1124	1129	1130	1145	1189
32	526	536	551	541	548	864
35	551	533	558	555	557	883
37	621	623	626	623	625	1367
16	545	549	553	548	549	634
17	570	570	571	568	565	1022

Table 6
Duct Wall Temperature
for Various Film Coolant Flow Rate

D. ANALYSIS

To interpret the results obtained from these tests, the one-dimensional analysis suggested in Reference⁽⁸⁾ can be used. This analysis suggests that once the film-cooling layer has been introduced and attached to the wall, the shear forces from the free-stream gas, plus any other forces (such as gravitational and pressure gradients), cause the fluid to flow along the wall. So long as the film-coolant flow remains laminar, only the energy interchange between the hot gases and the coolant needs to be considered. This is not a completely correct statement, but is adequate as a good basis for comparing the performance of coolants.

The energy transferred from the hot gases is absorbed by sensible heat of the liquid, by its heat of vaporization, and by sensible heat of the vapors as they are heated in the boundary layer. For the case of no-energy transferred through the film cooling layer, this energy balance can be stated as in Equations (15), (16), and (17).

The heat absorbed by the liquid film coolant is:

$$\dot{Q} = \dot{w} \left[\Delta h_v + C_{pL} (T_v - T_i) + \beta C_{pv} (T_r - T_v) \right] \quad (15)$$

The heat transferred to the surface of the liquid film is:

$$\dot{Q}_O = \pi D L \left[C_h (T_b - T_O) + Q_r \right] \quad (16)$$

Since $\dot{Q} = \dot{Q}_O$

$$\dot{w} = \frac{\pi D L \left[C_h (T_r - T_v) + Q_r \right]}{\Delta h_v + C_{pL} (T_v - T_i) + \beta C_{pv} (T_r - T_v)} \quad (17)$$

Once the amount of cooling water needed has been established (by Equation 17), the velocity through the inlet holes must be determined. There

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(8) A. R. Graham, An Experimental and Theoretical Investigation of Film Cooling of Rocket Motors, Purdue University, Report F-57-3, October 1957.

is a maximum velocity known as the "critical velocity" that should not be exceeded. This "critical velocity" is the velocity at which, if exceeded, the cooling water penetrates the gas stream and no longer adheres to the wall of the section that is to be cooled.

This "critical velocity" varies with the location of the inlet holes, and with the angle of injection. The more tangential the inlet stream becomes, as referenced to the gas flow, the higher the critical velocity can be.

The following equation is used to find the critical velocity of perpendicular injection holes:

$$\frac{\rho_b}{\rho_l} \frac{V_b^2}{V^{*2}} = (7.65 \times 10^{-5}) (Re_b)^{.79} (R_e^* C_a^*)^{.395} \quad (18)$$

where

$$R_e^* = \frac{\rho V^* D}{\mu}$$

$$C_a^* = \frac{\rho_l V^{*2}}{P_b - P_p}$$

As is apparent from Equation (18), both R_e^* and C_a^* contain V^* terms. It is therefore necessary to assume a critical velocity for the R_e^* and C_a^* terms and solve the equation. Iteration will be necessary until the correct value of V^* is assumed.

An additional restraint is imposed on the injection conditions by the requirement that the liquid layer must remain laminar for Equation (17) to be valid. The data from Ref. ⁽⁸⁾ indicates that the Reynolds number of the film must be below 350 for laminar flow to exist and that, above this value, the efficiency of cooling drops off as the 0.40 power of the coolant flow rate. Later work, as discussed in Ref. ⁽⁹⁾ has shown that other considerations

⁽⁸⁾ A. R. Graham, An Experimental and Theoretical Investigation of Film Cooling of Rocket Motors, Purdue University Report F-57-3, October 1957.

⁽⁹⁾ R. H. Sabersky, "Recent Developments in Convective Heat Transfer," ARS Journal, p. 325, May 1959.

influence the transition point, but again the simplification provides comparative results. The Reynolds number of the coolant just downstream of the injection point is defined as

$$\text{Re}_\delta = \frac{\dot{w} \delta}{\pi D \delta \mu_v} = \frac{\dot{w}}{\pi D \mu_v} \quad (19)$$

When this constraint of a film Reynolds number below 350 is applied to Equation (17), the maximum length (for 100% efficiency) that can be film cooled by a single set of injection orifices is defined, and can be stated as

$$L_{\max} = \frac{[\Delta h_v + C_{pl} (T_v - T_i) + \beta C_{pv} (T_r - T_v)]}{C_h (T_r - T_v)} \times 350 \mu_v \quad (20)$$

The length, $L_{\max}$, is close to the true maximum length that can be film cooled because the turbulence losses increase rapidly once the flow rate, as defined by Equations (17) and (20), has been exceeded. In addition, the use of these equations really gives an area ($\pi D L_{\max}$) that is cooled, rather than a true length, since the equations were developed on a one-dimensional basis.

When the free-stream flow of hot gases is not one-dimensional, the flow of film coolant will follow the gas flow, which provides the major forces acting on the coolant layer. In the case of the duct elbow, this results in a major part of the coolant flow being transferred to the low-heat-transfer region on the side and upper surface of the bend.

The one-dimensional concept is therefore unsuitable to predict the behavior of water as a film coolant in the scale model duct.

However, the average film cooled length of 11-in, at a coolant flow rate of 0.3 lb/sec, can be compared to a value to 18 in. predicted by Equation (20) for the scale-model conditions. Since the film Reynolds number for these conditions was 800, or 2.3 times the maximum value for 100% efficiency, therefore the efficiency should be roughly $2.3^{-0.4}$, or 70%. This compares to 11/18, or 62% observed during the tests.

The test results therefore are qualitatively in agreement with the one-dimensional theory, but further application, to determine the section of the model ejector which will actually be cooled by the film coolant, will require a much more exact analysis.

Applying the analysis to the full-scale Test Cell "D" ejector for the NERVA engine, 3 in. will be the maximum length that can be film cooled with water at 100% efficiency. The ratio of film cooled length to duct diameter is therefore reduced from 3.8 to 0.06, so that the gas flow in the full-scale duct will be much closer to one-dimensional approximation than in the case of the model duct.

E. CONCLUSIONS AND RECOMMENDATIONS

The inability of the film coolant to pass through the high-heat-transfer region on the bottom side of the duct could be due to the following causes. First, the total area film cooled is in qualitative agreement with the one-dimensional theory developed in Ref.⁽⁸⁾, so that the scale-model flow dynamics could be responsible for the inability to cool the desired area. Secondly, the strong shock structure in the high-heat-transfer area could have been responsible and caused the liquid film to detach from the wall. This is not believed to be the actual cause, but the possibility cannot be eliminated.

It is recommended that a more precise analysis and an extended film cooling test program be undertaken because of the following reasons;

1. In a supersonic duct, film cooling changes both the flow and shock patterns of the gas, thus preventing use of data obtained in a smooth dry duct for prediction of maximum heat fluxes and critical areas in a duct which employs film cooling.
2. The film coolant coverage in the elbow area is difficult to predict because of the shock system. A method of cooling the strong shock location must be investigated to verify any proposed method of cooling this region.
3. For the full-scale ejector, other fluids exhibit more favorable characteristics for a film coolant than does water. For example, a heavy hydrocarbon can theoretically cool a 36-in. length while water can cool a 3-in. length; therefore the use of hydrocarbon coolant would be more suitable from a thermal standpoint.
4. An accurate scaling method must be determined to permit accurate extrapolation of test results to full-size design. This is essential if maximum reliability is to be expected from the Test Cell "D" ejector duct system.

SECTION VII

SECONDARY EJECTOR TESTS

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VII. SECONDARY EJECTOR TESTS

A. GENERAL DISCUSSION

The nuclear engines that will be tested in Test Cell "D" are intended for use in an upper-stage vehicle whose operational environment will approach a perfect vacuum. The nozzles for these engines will therefore have large expansion area ratios, and will require low cell pressures before the nozzle will flow full. In addition, the turbopump system for the engine will probably be required to "bootstrap" the engine during the initial startup transient, and will require a low cell pressure to simulate the operational pressure ratio across the turbine driving the propellant pump. For engine cycles which will utilize energy losses in the nozzle, a further need for low cell pressures will exist, so that the operational conditions of energy release in the nozzle can be simulated during the initial transient.

As compared to a chemical rocket engine, a nuclear rocket engine requires a great deal of time to reach steady-state operation, thus accentuating the need for a low cell pressure. During this starting transient, or until the engine reaches 82% of full power (see Figure 20), the nozzle will not flow full. Because of the lightweight construction of the nozzle expansion skirt, it is desirable to have the exhaust gases separate at the highest possible value of A/A^* . This can be accomplished with the use of a secondary ejector.⁽¹⁰⁾

B. TEST METHOD

The lack of information on secondary ejector operation under Test Cell "D" conditions necessitated testing as the method of evaluation of the secondary ejector system for the duct. The first secondary ejector designs to be tested maintained a constant duct diameter and utilized the secondary ejector gases by injection into the duct at an angle. These configurations are shown in line A of Figure 39, and in Figure 40. Modifications to the designs were then

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⁽¹⁰⁾ A secondary ejector is a means of providing low cell pressure when the primary nozzle (nuclear engine) is not operating at a sufficient chamber pressure to provide this low cell pressure.

made during the testing program to improve performance by injection of the secondary fluid at a smaller angle with the ejector center line. These modifications are shown in lines B and C of Figure 39. A second secondary ejector model (Figure 41) was fabricated so that further changes could be made with a minimum of effort and was used for the tests, with the configurations of line D, Figure 39, and for the parallel injection tests, with the configuration of Figure 42.

Secondary ejector design information was obtained by varying the geometry of the nozzle, as shown in Figures 39 and 42 and varying the ratio of $(L/D)_E$ from the secondary ejector nozzle to the ejector exit. For each of these design changes, the secondary-ejector nozzle-chamber pressure was varied from 75 to 250 psia in 25 psi increments, with the purpose of determining the nozzle-chamber pressure and resulting cell pressure existing at "start condition". The primary nozzle was not operating for these tests.

SECONDARY EJECTOR NOZZLE	A_D/A_s^*	A_{se}/A_s^*	ϕ	β	θ	λ	L/D ℓ	P_v/P_a min.
A	13.15	3.82	35	24	46	22	10.28	.45
B	10.35	2.34	30	14	46	32	10.28	.34
C	10.15	1.87	19.5	14	25	11	10.28	.292
D	10.66	2.62	12.5	5	20	15	10.28	.132 ①
Da	10.66	2.62	12.5	5	20	15	8.13	.166 ①
Db	10.66	2.62	12.5	5	20	15	5.76	.177 ①

① EJECTOR STARTED

Figure 39
Secondary Ejector Test Model Configuration
Angular Injection

Photo
1061-335

Figure 40
Secondary Eject Models A,B,C

Photo
1161-229

Figure 41
Secondary Ejector Model D

SECONDARY EJECTOR NOZZLE	A_{D_1}/A_s^*	A_{D_2}/A_s^*	A_{se}/A_s^*	λ	L/D_2 ϕ	P_v/P_a min.
E	10.66	15.5	5	40°	8.16	.0442

Figure 42
Secondary Ejector Test Model Configuration
Parallel Injection

C. TEST RESULTS

The results of the secondary ejector tests indicate that, when a peripheral entrance is used, parallelism of the gas flow to the center-line of the ejector has a direct relationship to resulting cell pressure; the more parallel the flow, the lower will be the pressure. These results are shown in Figure 43.

Although the resulting cell pressure was reduced for each case (A to B to C to D), a start condition (P_v/P_{sc} remained constant after minimum P_v was obtained) was not exhibited until the nozzle centerline angle was 12.5° .

The experimental performance of a secondary ejector, as compared with the theoretical performance, is shown in Figure 44. This figure indicates that, although the starting pressure was approximately the same (10.5) for both experimental and theoretical performance, the resulting experimental cell-pressure ratio was 0.14, as compared to the theoretical value of 0.07.

Visual observation of the flow from the secondary ejector nozzle C (Test 8C) was made by removing the upstream portion of the duct and injecting small quantities of water into the nitrogen inlet line. By leaving the upstream portion of the duct open to the atmosphere just prior to the secondary ejector nozzle, the secondary gas flow was observed to converge downstream of the inlet. The upstream portion was then sealed with plexiglass to make the secondary ejector non pumping, and the gas flow was observed to converge downstream at such angles, that when P_{sc}/P_a was less than 10.4, there was no backflow; but when P_{sc}/P_a was increased to values greater than 10.4, backflow existed, and raised the cell pressure. This is shown graphically in Figure 43.

The secondary ejector nozzle location was varied to change the effective value of the L/D ratio. These results are shown in Figure 45, and indicate that an increase in the L/D beyond a certain point will cause an

increase in the value of $(P_{sc}/P_a)_{start}$ (Figure 46). Similar results are exhibited by Figure 6 in Ref. ⁽¹¹⁾, "Effect of ejector overall-length-to-diameter ratio on ejector minimum starting-pressure ratio for straight-tube ejectors."

A test was made in which a stepped portion in the duct allowed injection of the secondary ejector nozzle gases parallel to the duct centerline (see Fig. 42). The result of this test is shown in Figure 47, and indicates the parallelism of the secondary ejector nozzle gases to the centerline of the duct has a direct relationship to the resulting cell pressure; the more parallel the flow, the lower will be the pressure. The value of P_{sc} required for start in this test was greater than for Model D, due to the larger area ratio, A_d/A_s^* .

⁽¹¹⁾ W. L. Jones, H. G. Price, Jr., and C. F. Lorenzo, Experimental Study of Zero-Flow Ejectors Using Gaseous Nitrogen, NASA Technical Note D-203, March 1960.

Figure 43
Cell Pressure vs Chamber Pressure
Various Secondary Ejector Configurations

Figure 44
Experimental and Theoretical Performance
Secondary Ejector Model D

Figure 45
Performance of Secondary Ejector Nozzle
with Different Ratios of L/D

Figure 46
Effect of L/D on (P_{sc}/P_2) Start
for Secondary Ejector Model D

Figure 47
Performance of Secondary Ejector Model E
Parallel Injection

D. CONCLUSIONS AND RECOMMENDATIONS

1. Nozzle angle must be 12.5° or less to provide an ejector "start" condition.
2. Parallel injection of the secondary fluid exhibits the most satisfactory secondary ejector performance, although a slight increase in the value of $(P_{sc}/P_a)_{start}$ was required.
3. An increase in the L/D ratio beyond a certain value will cause an increase in the chamber pressure required for start conditions.
4. It is recommended that further tests be made with the Model E Secondary ejector to obtain sufficient information to make possible the inclusion of this design in any future nuclear-engine test-stand work.

SECTION VIII

PURGE TESTS

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VIII. PURGE TESTS

A. GENERAL DISCUSSION

Before the nuclear engine can be started, the oxygen content in the environmental cell and the ejector must be reduced to a safe limit to prevent a hydrogen-air explosion. Reference 12 shows that the flammable limit for hydrogen-air is 4 to 75% H_2 by volume, and for hydrogen oxygen, is 4 to 96%. The ignition temperature of hydrogen-air at 1 atm is 1061 to 1065°F, according to Reference 12 and is shown in Figure 48 according to Reference 13. To prevent a fire or explosion, it is reasonable to decrease the oxygen content to some value below 4% by volume before the engine is started.

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- (12) Chandless, E. L., "Handling and Storage of Liquid Hydrogen", Space/Aeronautics Research and Development Handbook, Conover-Mast Publications, Inc., New York, 1960, p. k-12
- (13) Baumeister, T., Editor, Mechanical Engineers Handbook, Mc-Graw-Hill New York, 1958, p. 9-127.

Reference

Baumeister, T., Editor, Mechanical Engineers Handbook,
McGraw - Hill Book Company, New York, 1958, p. 9-127.

Figure 48
Ignition Temperature of
 H_2 - Air Mixture

B. TEST METHOD

Small quantities of nitrogen were introduced into the environmental cell by means of a special injection nozzle located in the top of the cell (see Figure 49).

Samples of the atmosphere in the environmental cell were taken in 50-cc glass bottles. These gas samples were analyzed for O_2 content by a process of gas chromatography.

Purge requirements were determined by pumping down the cell with the secondary ejector, flowing nitrogen into the cell to reduce the oxygen content, and sampling the gases in the cell at given time intervals. Four samples were obtained for each test. The nitrogen purge flow rate for Run 88 was 0.002 lb/sec, and the samples were taken at approximate intervals of 3 seconds. For Run 89 the flow rate was 0.0032 lb/sec, and the samples were taken at approximate intervals of 2 seconds. A correction for the length of the inlet line to the sampling bottles was made by calculating the volume of the inlet lines from the cell to stopcock of the sampling bottle, and assuming this volume to be at cell pressure, but not effected by the nitrogen purge gas. Therefore, when a sample of cell gas was taken, a small quantity of gas, equal to the volume of the inlet line, contained the normal amount of atmospheric oxygen.

Figure 49
Ejector System Purge

C. TEST RESULTS

The results of runs 88 and 89 are plotted in Figure 50, and are tabulated below.

PURGE TEST RESULTS

<u>Test No.</u>	<u>Run No.</u>	<u>time (sec)</u>	<u>$\dot{w}_n$</u>	<u>T₂</u>	<u>% O₂</u>	<u>$\frac{\text{Vol N}_2}{\text{Vol cell}}$</u>
9	88	0	.00260	525	20.9	0
		3.29	.00195	525	20.6	.216
		6.95	.00197	523	14.95	.422
		11.11	.00200	522	12.35	.660
		15.88	-	-	-	-
9	89	0	.00393	525	20.9	0
		2.49	.00307	524	15.6	.261
		5.64	.00310	520	9.2	.550
		8.95	.00314	518	2.52	.859
		13.18	.00318	517	2	1.259

Figure 50
Volume of Nitrogen Required
to Reduce the Percentage of Oxygen in the Test Cell Atmosphere

D. ANALYSIS

Two primary models can be considered: slug flow (that is, the purge gas pushes the air from the environment that is to be purged with no convective mixing or no diffusion) and the assumption of perfect mixing.

Slug flow with no diffusion at the interface will result in minimum purge gas for low O_2 concentration, as shown in Figure 50, assuming that the interface is normal to the flow. Slug flow without diffusion cannot be realized, but may be closely approached if the stay time of the interface in the environment to be purged is small. Mass diffusion is analogous to thermal diffusion, and the rate of diffusion is a function of the square root of the stay time and the diffusivity, which is a function of pressure, temperature, and the molecular weight.

The following analysis will give the theoretical amount of an inert gas that must be introduced to decrease the oxygen content to any desired level if instantaneous perfect mixing is assumed. This analysis is based upon the assumptions that the pressure throughout the system being analyzed is constant and does not change with time, that the fluids behave as perfect gases, and that the oxygen content by volume is 20.9% at time zero.

The conservation of mass in the system is

$$W = (\text{oxygen entering at 1}) - (\text{oxygen leaving at 3}).$$

The amount of oxygen entering at 1 is zero. At time t , there is V_2 ft³ of gas containing $\frac{W}{V_2}$ lb/ft³ of oxygen. The gas leaves 3 at the rate of $\dot{V}_3$ ft³/sec, so that $\dot{V}_3 \frac{W}{V_2}$ lb of oxygen leaves 3 each sec, and in time (t) sec there will be

$$\text{amount oxygen leaving} = \dot{V}_3 \frac{W}{V_2} \Delta t \quad (22)$$

Hence, from Equations (21) and (22)

$$\Delta W = -\dot{V}_3 \frac{W}{V_2} \Delta t \quad (23)$$

With pressure, temperature and V_2 are constant at any given time, and when

$$R_{N_2} = R_{(\text{air} + N_2)} \quad (24A)$$

then

$$\dot{V}_1 = \dot{V}_3 \quad (24B)$$

where the volumetric flow rate at station 1 is defined as:

$$\dot{V}_1 = \frac{W_{N_2} R_{N_2} T_2}{P_2} \quad (25)$$

Combining (23) and (24B), and dividing by W ,

$$\frac{\Delta W}{W} = - \frac{\dot{V}_1}{V_2} \Delta t \quad (26)$$

If V_{O_2} is defined as the partial volume of V_2 occupied by the oxygen, then equation (26) becomes:

$$\frac{\Delta V_{O_2}}{V_{O_2}} = - \frac{\dot{V}_1}{V_2} \Delta t \quad (27)$$

The cell temperature will not remain constant during the purge, but can be calculated, if V_2 is defined as

$$V_2 = V_{N_2} + V_a \quad (28)$$

and if T_2 is defined as

$$T_2 = \frac{V_{N_2} T_{N_2} + V_a T_a}{V_2} \quad (29)$$

The partial volume of V_2 occupied by air is

$$V_a = \frac{\% O_2}{20.9} V_2 \quad (30)$$

Solving equations (28), (29), and (30) simultaneously to eliminate V_{N_2} and V_a yields

$$T_2 = \left(1 - \frac{\% O_2}{20.9}\right) T_{N_2} + \frac{\% O_2}{20.9} T_a \quad (31)$$

where

$$\begin{aligned} T_a &= \text{initial temperature of air in cell (ambient)} \\ T_{N_2} &= T_1 \end{aligned}$$

If $T_1 = T_a$, then T_2 is constant with respect to time. If the purge flow $\dot{w}_{N_2}$ is constant, then $\dot{V}_1$ is constant and independent of time. See Equation (25).

Taking the limit of equation (7), we obtain:

$$\frac{d V_{O_2}}{V_{O_2}} = - \frac{\dot{V}_1}{V_2} dt \quad (32)$$

Which, after integration becomes

$$\ln \frac{V_{O_2}}{.209 V_2} = \frac{\dot{V}_1}{V_2} t = \frac{V_1}{V_2} \quad (33)$$

when $\dot{V}_1$ is constant and % O_2 by volume is 20.9 at time zero. Equation (33) in exponential form is

$$\frac{V_{O_2}}{V_2} = .209 e^{-\frac{V_1}{V_2}} \quad (34)$$

which is equivalent to

$$\% O_2 = 20.9 e^{-\frac{V_1}{V_2}} \quad (35)$$

where

$$V_1 = \frac{\dot{w}_{N_2} t R_{N_2} T_2}{P_2} \quad (36)$$

If T_1 is not equal to T_a , then equation (27) must be solved by taking incremental time elements.

The test results for run number 88 and 89 indicate that neither slug flow with no diffusion, nor perfect mixing, is realized. For a conservative estimate of the amount of purge gas needed, perfect mixing can be assumed if the purge nozzles are arranged in such a manner that no stagnate pockets exist.

190 lb of nitrogen was needed to reduce the oxygen content of the environmental cell and the ejector up to the secondary ejector nozzle (7460 ft³) to 1% by volume (a conservative estimate). The temperature of the nitrogen and

air was assumed to be 540°R , and the environmental pressure was assumed to be 1.7 psia. If the pressure was assumed to be 1 psia, then the amount of nitrogen needed would be 110 lb (see Equations (35) and (36) or Figure 50).

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E. CONCLUSIONS AND RECOMMENDATIONS

A conservative estimate for the amount of purge gas required is 190 lb, if the purge nozzles are arranged in such manner that no stagnate pockets exist. It would be possible, with an extended test program, to reduce this amount of nitrogen to a smaller value, but considering the relatively small cost of nitrogen, this does not seem necessary.

It is recommended that a checkout run be made with the selected purge hardware at its design flow rate to determine the actual amount of N_2 purge needed to obtain a given O_2 concentration for the full-scale system. This could be done in the same manner as in the test program, and would be relatively simple and inexpensive to perform.

The purge nozzles should be arranged in such manner that all the air in the environmental cell is moved (no stagnate pockets).

SECTION IX

STARTUP TESTS

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IX. STARTUP TESTS

A. GENERAL DISCUSSION

As discussed in Section VII, the primary purpose of the secondary ejector is to provide sufficient altitude simulation so that the nozzle flows full at a low chamber pressure. When operated alone, the secondary ejector will provide sufficient altitude simulation. However, when the engine is started and there is a combined flow from the secondary ejector and the engine, there can be an increase in cell pressure with an increase in engine chamber pressure.

It is desirable to keep the cell pressure as low as possible during all phases of engine startup in order that the nozzle separation point may be kept at an area ratio as high as possible during the initial start up transient; it is also desirable for the nozzle to flow full at as low a chamber pressure as possible.

The self-imposed goal was for the environmental pressure to simulate 60,000 ft or greater, but this goal was not obtained with the secondary ejector nozzle tested.

B. TEST METHOD

The secondary ejector was started at the desired chamber pressure. The engine was then started at a chamber pressure of 50 psia and allowed to run at this pressure for approximately 5 seconds, and the engine-chamber pressure was increased in steps of 100 psi until 550 psia was reached. A stabilization time of approximately 5 seconds was allowed for each engine-chamber pressure.

This procedure was used to evaluate secondary-ejector-chamber pressures of 50, 114, and 162 psia using the Model D scale model ejector (see Section VII, Figure 39).

For these tests, the primary nozzle gases were not heated, but were used at ambient temperature to simplify analysis of the test results. Pressure measurements were made in the same locations as in the gas dynamics tests and the secondary ejector tests.

C. TEST RESULTS

1. Environmental Cell Pressure

The environmental-cell pressure was found to continually increase with increasing primary chamber pressure when the secondary-nozzle chamber pressure was 162 psia (Figure 51), while for a secondary-nozzle chamber pressure of 114 psia, the cell pressure increased and then leveled out at about 5.4 psia until the primary nozzle could pull the cell in (start) by itself ($P_{sc} = 0$). For lower values (50 psia) of secondary nozzle chamber pressures, the cell pressure increased to a maximum, and then decreased with increasing primary chamber pressure.

Figure 51
 Cell Pressure vs Nozzle Chamber Pressure
 for Various Values of Secondary Ejector Chamber Pressure
 N_2 Experimental Results
 (NERVA Ejector Model)

2. Static Wall Pressure

The wall-pressure profile for combined flow is shown in Figures 52 and 53. The gas leaving the primary nozzle increased in pressure due to turbulent mixing until the secondary-ejector-nozzle, back-pressure, P_2 , was reached. The static pressure then remained constant up to an L/D of 8.5, which was at the location of the secondary ejector nozzle. The secondary fluid was introduced with supersonic velocity at this location and the gases underwent a shocking process, followed by turbulent mixing, which brought the static pressure up to the ejector exit pressure, P_4 . This process held until the primary chamber pressure exceeded approximately 500 psia. At these pressures, the static pressure increased as before due to turbulent mixing, but to a larger pressure than P_2 (Figure 52 and 53). The decrease in pressure from the maximum to P_2 was too large to be caused from frictional losses, and at this time this flow phenomenon cannot be explained.

Figure 52
Ejector Wall Static Pressures vs L/D Along Centerline
During Startup ($P_{sc} = 162$ psia)

Figure 53
Ejector Wall Static Pressures vs L/D Along Centerline
During Startup ($P_{sc} = 114$ psia)

3. Ejector Exit Total Pressure

During the test program, the total pressure at the center of the ejector exit P_{7T} , was measured, and is shown in Figure 54. The sharp jump shown when $P_{sc} = 162$ psia is attributed to a supersonic core caused by incomplete mixing of primary and secondary gases.

Figure 54
Total Pressure at Ejector Exit, P_{7t} , vs
Combined Flow Rate, $\dot{w} + \dot{w}_s$
(NERVA Injector Model)

D. ANALYSIS

When the secondary ejector nozzle chamber pressure P_{sc} , is sufficiently large, supersonic flow exists from the nozzle and the induced flow (Primary), $\dot{w}$, is initially entrained at high speed. The flow then ideally mixes and leaves the ejector as a uniform subsonic stream. The local static pressures of the two gases on the surface of the flow boundary (between 2 and 3 in Figure 55) are equal due to pressure continuity in the absence of strong shocks. The expansion waves originating within the jet are reflected as compression waves which unite into a shock system at the flow boundary, and nozzle the mean pressures of the two gases, at any plane between 2 and 3, are not necessarily equal.

The viscous action between the two gases brings M_2 up to M_3 , but if only the initial and the final states are considered, the pumping action of an ejector can be determined by the laws of internal gas dynamics of perfect fluids.

Figure 55
Simplified Model Used
for Mixed Flow Analysis

1. Analysis to Determine Secondary Ejector Nozzle Back Pressure

If the frictional losses between station 2 and 4 are known, or can be determined, and if perfect mixing is assumed, then a momentum balance between station 2 and 4 (Figure 55) can be written as

$$\left(\frac{RT\dot{w}^2}{gPA} \right)_2 + P_2 A_2 + F_s = \left(\frac{\bar{R}\bar{T} (\dot{w} + \dot{w}_s)^2}{gPA} \right)_4 + P_4 A_4 + Z_{2-4} \quad (37)$$

This can be used to determine P_2 as a function of $\dot{w}$ for given secondary ejector nozzle conditions.

The mean engineering gas constant, $\bar{R}$, in Equation (37) is found by the relationship

$$\bar{R} = \frac{\dot{w}R + \dot{w}_s R_s}{\dot{w} + \dot{w}_s} \quad (38)$$

The mean total temperature at station 4 (Figure 55) is determined from the assumption of no chemical reaction between the two, gases and from the energy equation

$$\dot{w} C_p T_t + \dot{w}_s C_{ps} T_{ts} = (\dot{w} + \dot{w}_s) \bar{C}_p \bar{T}_t + Q_{2-4} \quad (39)$$

where

$$\bar{C}_p = \frac{\dot{w}C_p + \dot{w}_s C_{ps}}{\dot{w} + \dot{w}_s} \quad (40)$$

and, with the assumption of adiabatic flow,

$$Q_{2-4} = 0 \quad (41)$$

The startup tests used unheated nitrogen for both fluids, therefore Equations (38) through (41) need not be used to analyze the test results.

The theoretical back-pressure, P_2 , from Equation (37), and the test results are in fairly good agreement (Figure 56 and 57) up to a nozzle chamber pressure, P_c , of 250 psia.

If perfect mixing cannot be assumed, or if the frictional losses cannot be determined with sufficient accuracy, then a method similar to that of J. Fabri (14) can be used. The advantage of this analysis is that perfect mixing is not required, and the frictional losses between the stations considered are much smaller and could more easily be justifiably neglected. The continuity relationship between Stations 2 and 3 (see Figure 55) is

$$P_2 A_2 V_2 = P_3 A_3 V_3 \quad (42)$$

which, with the perfect gas and Mach number relationship, becomes

$$A_2 P_2 M_2 \left[\frac{\gamma g}{RT_2} \right]^{1/2} = A_3 P_3 M_3 \left[\frac{\gamma g}{RT_3} \right]^{1/2} \quad (43)$$

If the flow is isentropic, the continuity relationship between Stations 2 and 3 become

$$\frac{A_3}{A_2} = \frac{M_2}{M_3} \left[\frac{1 + \frac{\gamma-1}{2} M_2^2}{1 + \frac{\gamma-1}{2} M_3^2} \right]^{-\frac{\gamma+1}{2(\gamma-1)}} \quad (44)$$

and

$$\frac{A_3}{A_2} = \frac{M_2}{M_3} \left[\frac{1 + \frac{\gamma-1}{2} M_2^2}{1 + \frac{\gamma-1}{2} M_3^2} \right]^{-\frac{\gamma+1}{2(\gamma-1)}} \quad (45)$$

The isentropic pressure relationships between Stations 2 and 3 are

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(14) J. Fabri and J. Paulon, Theory and Experiments on Supersonic Air-To-Air Ejectors, NACA Technical Memorandum 1410, September 1958.

$$\frac{P_{s3}}{P_{s2}} = \frac{\left[1 + \frac{\gamma_s - 1}{2} M_{s3}^2\right]}{\left[1 + \frac{\gamma_s - 1}{2} M_{s2}^2\right]} - \frac{\gamma_s}{\gamma_s - 1} \quad (46)$$

and

$$\frac{P_3}{P_2} = \frac{\left[1 + \frac{\gamma - 1}{2} M_3^2\right]}{\left[1 + \frac{\gamma - 1}{2} M_2^2\right]} - \frac{\gamma}{\gamma - 1} \quad (47)$$

A momentum balance between Stations 2 and 3 yields

$$\begin{aligned} & P_2 A_2 (1 + \gamma M_2^2) + P_{s2} A_{s2} \cos \phi (1 + \gamma_s M_{s2}^2) - \int_0^{A_{SZ}} P_m \cos \phi \, dA_m \\ &= P_3 A_3 (1 + \gamma M_3^2) + P_{s3} A_{s3} (1 + \gamma_s M_{s3}^2) + \int_{L_2=0}^{L_3} \frac{f P A M \gamma}{2D} \, dL \end{aligned} \quad (48)$$

at plane 3

$$A_3 = A_2 - A_{s3} \quad (49)$$

For the condition of frictionless and optimum operation

($M_3 = 1$) and for given values of A_{s2} , A_2 , P_{s2} , M_{s2} , γ_2 , γ , and ϕ , the equations (41) to (49) with the variables A_{s3} , A_3 , P_2 , P_{s3} , P_3 , M_2 , and M_{s3} , can be solved for given values of M_2 .

The flow rate $\dot{w}$ can be determined if T_t is defined from Equations (41) to (49), and the continuity equation, as shown below

$$\dot{w} = P_2 A_2 M_2 \left[\frac{\gamma g (1 + \frac{\gamma - 1}{2} M_2^2)}{R T_t} \right]^{1/2} \quad (50)$$

The primary nozzle chamber pressure P_c can now be determined from the isentropic relationship

$$P_c = \frac{\dot{w}}{A^*} \left[\frac{R T_t}{\gamma g \left(\frac{2}{\gamma+1} \right)^{\frac{\gamma+1}{\gamma-1}}} \right]^{1/2} \quad (51)$$

where A^* = primary nozzle throat area.

The following procedure can be used to determine P_c as a function of P_2 , if A_{s2} , A_2 , P_{s2} , P_{sc} , M_{s2} , γ_s , γ , ϕ , and T_t are known:

- a. For a fixed value of $\frac{A_{s2}}{A_{s*} P_{sc}}$ find $\frac{P_{s2}}{P_{sc}}$ and M_{s2} from Ref. (15)
- b. For $M_3 = 1$, $\frac{A_3}{A^*} = 1$, and $\frac{P_3}{P_t}$ can be found from Ref. (15)
- c. Assume a value of M_2 and find $\frac{P_2}{P_t}$ and $\frac{A_2}{A^*}$ from Ref. (15)
- d. Solve for P_3 as a function of P_2 and A_3 from the identities

$$A_3 = A_2 \frac{A_3/A^*}{A_2/A^*} = \frac{A_2}{A_2/A^*} \quad (52)$$

and

$$P_3 = \frac{P_3/P_t}{P_2/P_t} P_2 = KP_2 \quad (53)$$

- e. Solve for A_{s3} by Equation (49)
- f. Determine A_{s3}/A_{s*} from the identity

$$\frac{A_{s3}}{A_{s*}} = \left(\frac{A_{s2}}{A_{s*}} \right) \frac{A_{s3}}{A_{s2}} \quad (54)$$

- g. Now that A_{s3}/A_{s*} is known, M_{s3} and P_{s3}/P_{sc} can be found from Ref. (15). The pressure P_{s3} is then determined from the identity

$$P_{s3} = P_{sc} (P_{s3}/P_{sc}) \quad (55)$$

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(15) C. J. Wang, J. B. Peterson, R. Anderson, Gas Flow Tables, GM-TR-154, Space Technology Laboratories Inc., March 1957.

h. We now know A_{s3} , A_3 , P_{s3} , M_2 , M_3 , and P_3 as functions of P_2 . The pressures P_3 and P_2 can be determined from Equation (48) and the above knowns, if the frictional loss between stations 2 and 3 is considered 0, and if

$$\int_0^{A_{s2}} P_m \cos \phi d A_m \text{ is defined.}$$

g. The flow rate $\dot{w}$ and primary nozzle chamber pressure P_c can now be determined from Equations (50) and (51).

k. Assume a different value of M_2 and repeat steps a through j.

The above procedure is valid for $A_{s3} - A_{s2}$. For the condition of $A_{s3} = A_{s2}$, the primary flow $\dot{w}$ is limited only by the aerodynamic blocking of the area A_3 . The area A_{s3} is then equal to A_{s2} , and M_3 is still equal to 1 for optimum operation. For these conditions, P_c/P_2 is constant and can be determined from Equations (50) and (51)

$$\frac{P_c}{P_2} = \frac{A_2 M_2}{A^*} \left[\frac{1 + \frac{\gamma-1}{2} M_2^2}{\left(\frac{2}{\gamma+1}\right) \frac{\gamma+1}{\gamma-1}} \right]^{1/2} \quad (56)$$

where A^* = primary nozzle throat area M_2 can be found from Equation (45).

For a given geometry and secondary nozzle chamber conditions, P_c can be determined as a function of P_2 . It should be noted that P_2 should be determined as has been described in Section VII, when the induced flow $\dot{w}$ is 0.

- NOTES
1. NERVA ejector
  2. Secondary ejector nozzle-model D
  3. Test number 10a

Figure 56
 Secondary Ejector Nozzle Back Pressure, P_2 ,
 vs Primary Nozzle Chamber Pressure
 ($P_{sc} = 162$ psia)

- NOTES**
1. NERVA ejector
  2. Secondary ejector nozzle - model D
  3. Test number 10a

Figure 57
 Secondary Ejector Nozzle Back Pressure, P_2 ,
 vs Primary Nozzle Chamber Pressure
 ($P_{sc} = 114$ psia)

2. Analysis to Determine Environmental Pressure, P_v

To determine the environmental cell pressure P_v as a function of P_c and P_2 , the equations of continuity, energy, and momentum can be expressed between the nozzle flow-separation plane, station F, and station 2 (Figure 55). Fortini's analysis (Ref. 16, Case I) is used here with the assumption that the pressure force F_x shown in Figure 55 is $F_x = P_v (A_1 - A_2)$.

The quadratic equation

$$\frac{P_2}{P_F} = \frac{-b \pm [b^2 - 4ac]^{1/2}}{2a} \quad (57)$$

$$a = \left(\frac{A_2}{A_F}\right)^2 \left(\frac{\gamma+1}{\gamma-1}\right)$$

$$b = -\left(\frac{2}{\gamma-1}\right) \frac{A_2}{A_F} \left[\frac{P_v}{P_F} \left(\frac{A_2}{A_F} - 1\right) + 1 + \gamma M_F^2 \right]$$

$$c = \frac{2\gamma^2}{\gamma-1} M_F^2 \left[1 + \frac{\gamma-1}{2} M_F^2 \right] - \left[\frac{P_v}{P_F} \frac{A_2}{A_F} - 1 + 1 + \gamma M_F^2 \right]^2$$

can be solved for P_v , once P_2 and P_c are known.

The ratio P_v/P_2 can be determined as a function of P_c/P_2 by the procedure outlined by Fortini⁽¹⁵⁾ which is presented below, to provide completeness of the report.

a. Assume various values of $M_F \leq M_e$ and obtain from Ref. (15) A^*/A_F and P_c/P_F . By using Green's data (Figures 58 and 59)⁽¹⁷⁾ for a cross-plot of P_v/P_F against P_c/P_F , obtain a value of P_v/P_F .

b. By fixing A_2/A^* , compute

$$(1) \quad A_2/A_F = (A_2/A^*) (A^*/A_F)$$

⁽¹⁶⁾ Anthony Fortini, Performance Investigation of a Nonpumping Rocket-Ejector System for Altitude Simulation, NASA Technical Note D-257, December 1959.

⁽¹⁷⁾ Leon Green, Jr., "Flow Separation in Rocket Nozzles", Journ. Am. Rocket Soc., 23, No. 1, January-February 1953, pp. 34-35

$$(2) \quad a, b, \text{ and } c$$

$$(3) \quad \frac{P_2}{P_F} = \frac{-b \pm [b^2 - 4ac]^{1/2}}{2a}$$

$$(4) \quad P_V/P_2 = (P_F/P_2) (P_V/P_F)$$

$$\text{and } P_C/P_2 = (P_C/P_F) (P_F/P_2)$$

c. Equation (57) results in double roots. The negative roots have negative values of P_V/P_2 and P_C/P_2 , which are physically impossible. The positive root for P_V/P_2 is also physically impossible.

The ratio P_V/P_2 as a function of P_C/P_2 is shown in Figure 60 for the given condition of

$$A_2/A^* = 35$$

$$\gamma = 1.4$$

conical primary nozzle

15° nozzle half-angle

Experimental data are available for the NERVA and Phoebus ejectors with reference to the cell-pressure ratio ($P_2/P_a = P_V/P_2$) as a function of nozzle-chamber-pressure ratio ($P_c/P_a = P_c/P_2$), and are shown in Figure 61 and 62. These figures can be used, as it is assumed that the performance of the ejectors will not be affected if the exit is at Station 2 instead of Station 4.

Figure 58
Nozzle Separation Pressure Ratio

$$\frac{P_f}{P_c} \quad \text{vs} \quad \frac{P_c}{P_v}$$

Figure 59
Nozzle Separation Pressure Ratio
 $\frac{P_v}{P_f}$ vs $\frac{P_c}{P_f}$

Figure 60
Theoretical Cell Pressure vs Chamber Pressure
as a Function of P_2
(See Figure 55)

Figure 61
Performance Comparison Between Hot and Cold-Flow Tests
NERVA Ejector Model

Figure 62
Performance Comparison Between Hot and Cold-Flow Tests
Phoebus Ejector Model

3. Analysis to Determine Duct Exit Conditions

For momentum balance, it is required that the exit static pressure, P_4 , and the exit momentum be estimated. During the test program the total pressure at the center of the ejector exit, P_{7T} (Figure 54) was measured, but the exit pressure was not measured. Consequently, it was necessary to relate the total pressure at the duct exit to the static pressure at this location on the basis that the total pressure at the center of the duct exit was representative of the complete flow.

For this purpose, the method of Reference 18, for graphical solution of compressible flow in a duct, was used. As long as the flow is subsonic at the duct exit, the exit static pressure, P_4 , will remain equal to the atmospheric pressure. However, the flow conditions during these tests were such that choked flow occurred in the duct in some cases, and therefore P_4 was higher than atmospheric.

To estimate P_4 for the choked-flow cases, the experimental data for total pressure, P_{7t} , was compared with the calculated total pressure for compressible duct flow with L/D ratio of 10, and a friction factor of 0.016. This comparison is shown in Figure 54, where it can be seen that for isothermal flow ($\gamma = 1.0$) the theory gives good agreement with test data for a secondary chamber pressure, P_{sc} , of 114 psia. The sharp jump shown for the case where $P_{sc} = 162$ psia is attributed to a supersonic core caused by incomplete mixing of primary and secondary gases. The observed total pressure data for the case where $P_{sc} = 114$ psia may also be explained by the fact that frictional losses are higher than predicted. To give agreements for a γ of 1.4, the frictional losses would have to be 4 times higher than first estimated. However, the test data indicated that, for either explanation, the exit static pressure, P_4 , became proportional to the total pressure, P_{7T} , whenever the total pressure exceeded 25 psia, and for that reason this value was used.

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(18) J. H. Perry, Chemical Engineers Handbook, McGraw-Hill, New York, 1950.

The losses due to friction can be approximated by the Darcy head-loss equation

$$h_L = f \frac{L}{D} \frac{V^2}{2g} \quad (58)$$

which can be converted from height to force units by the following relationship:

$$Z = h_L \rho A \quad (59)$$

Combining Equations (58) and (59), the perfect gas law and the continuity relationship, yields

$$Z = \frac{0.184 \mu^{.2} R T L}{2g P A^{0.8} D^{1.2} \dot{w}^{1.8}} \quad (60)$$

when

$$f = \frac{0.184}{R_e^{0.2}} \quad (\text{turbulent flow}) \quad (61)$$

Equation (60) shows that for turbulent flow the frictional losses are proportional to the flow rate raised to the 1.8 power.

A momentum balance between Stations 2 and 4 (see Figure 55)

$$\left(\frac{R T \dot{w}^2}{g P A} \right)_2 + P_2 A_2 + F_s = \left(\frac{R T (\dot{w} + \dot{w}_s)^2}{g P A} \right)_4 + P_4 A_4 + Z_{2-4} \quad (62)$$

and the test data from runs 108 and 110 (Figures IX-2 and IX-3) were used to determine the losses (Z) when the secondary ejector was operating. The results indicated that these losses, which were primarily due to frictional effects, were independent of the primary flow and were proportional to the secondary flow rate ($\dot{w}_s$) raised to the 1.8 power (Figure 63) rather than to the mixed flow rate ($\dot{w}_s + \dot{w}$).

The value of the frictional loss in the duct was approximately 5 times greater than the loss predicted by Equation (60) which is based on developed flow. There are several possible reasons for this high frictional loss; the leading-edge effect produced by the secondary fluid, and the compressive effect caused by the primary fluid. However, if the primary fluid influenced the effective flow area of the secondary fluid, a variation in frictional losses with primary flow would be expected. This was not observed during these tests, and hence makes this explanation questionable. A third, and more likely explanation is that the secondary ejector gases cannot expand fully before they are some distance downstream of the injection point, and hence would be expected to have a considerably higher frictional loss than would be predicted by developed flow equations. The fact that the frictional loss is in qualitative agreement with the loss required to explain the duct-exit total pressure measurements is an interesting comparison, even though about half of the data points were in a region (subsonic) where this relationship was not used.

Since the frictional loss is a boundary-layer effect, the test results indicate that the boundary layer is made up of the secondary ejector fluid, and hence shields the wall from the primary gas flow.

- NOTES 1. Secondary ejector nozzle D
2. Tests no. 10a and 10c

Figure 63
Ejector Frictional Loss vs Secondary Flow Rate

E. CONCLUSIONS AND RECOMMENDATIONS

1. Altitude starting simulation of the NERVA engine, combined with the explosion-free operating requirement, necessitates use of a secondary ejector for the duct; this ejector ideally should maintain altitude conditions in the environmental cell at all times during the start-up operation and the shut-down of the engine. Tests made with secondary ejectors in the deflector-duct model have thus far not accomplished the objective of maintaining a high-altitude environment. When operated alone, both secondary ejector models D and E provide sufficient altitude simulation; however, when accompanied by engine flow the cell pressure increases (with increasing nozzle chamber pressure) a sufficient amount to prevent the nozzle from flowing full. (This test was made for secondary ejector Model D, only.) Therefore, during engine startup the nozzle gases will not be fully expanded until the secondary ejector nozzle is shut off.

2. It is felt that secondary ejector Model E will allow the primary nozzle to flow full at a low-chamber pressure during the start-up transient. The reasons for incorporating Model D into the present Test Cell "D" design are given in Section XI, Design Considerations.

3. Incomplete mixing of primary and secondary gases causes a supersonic core of gas at the duct exit.

4. It is recommended that tests be run with different temperatures levels between the primary and secondary flows to check Equation (37) with this added variable, so there may be more confidence when scaling to the full-size ejectors.

5. A momentum balance between Stations 2 and 4 [Equation (37) or (48) and (50)] yields $\dot{w}$, from which P_c can be determined as a function of P_2 . Tests should be run to show the relationship between P_c/P_2 . This could be accomplished by removing the secondary ejector nozzle and the portion of the ejector down-stream of the secondary ejector, and varying the nozzle P_c , while measuring P_v and P_2 .

SECTION X

BACKFLOW ANALYSIS

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X. BACKFLOW ANALYSIS

A. GENERAL DISCUSSION

The use of hydrogen as a propellant for nuclear engines requires that air be excluded from the ejector system at all times during engine operation. Backflow of air into the ejector for any significant length cannot be tolerated, because of the explosive nature of the hydrogen-air mixture.

Three conditions under which backflow might occur are:

1. during shutdown, when P_c is small
2. when the shock pattern within the ejector breaks down at $(\frac{P_c}{P_a})_{start}$, under decreasing P_c/P_a (Note: this is shown by Figure 64. As P_c decreases, P_e also decreases, and P_v remains fairly constant. As soon as P_c reaches the value for $(P_c/P_a)_{start}$ under decreasing P_c/P_a , there is an instantaneous rise in both P_e and P_v , indicating that the cell pressure is no longer at a low value, and that the nozzle is no longer flowing full,
3. When supersonic flow exists at the ejector exit, as shown in Figure 65.

Supersonic flow throughout an ejector is discussed in (19). . . "It is known that boundary layers in adverse pressure gradients ($\frac{dP}{dx} > 0$) often exhibit fluctuating local backflows in the wall layers, even though the outer portion of the boundary layer behaves essentially like an installed boundary layer [see References (20), (21), and (22)]. In fact, in diffusing passages usually considered to operate in an 'unstalled' manner, small transitory stalls

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- (19) W. C. Reynolds, Evaluation of Proposed Model Experiments for Test Cell "D", Consultant Report, Feb. 1961.
- (20) N. Sivo, C. L. Meyer, D. J. Peters, Experimental Evaluation of Rocket Exhaust Diffusers for Altitude Simulation, NASA Technical Note 298, July 1960.
- (21) Kline, S. J., "On the Nature of Stall", Journal Basic Engineering, 81D TASME, September 1959.
- (22) Kline, S. J., "Some New Conceptions of the Mechanism of Stall in Turbulent Boundary Layers", JAS, 24, 6 June 1957.

are often in evidence (see Figure 66). These observations... indicate that the discharge of the Test Cell "D" ejector must be designed with a favorable pressure gradient $\frac{dP}{dx} < 0$. The straight ejector in subsonic flow has such characteristics. However in supersonic flow through a straight ejector, the boundary layer sees an adverse pressure gradient. A favorable pressure gradient can be obtained in supersonic flow by increasing the flow area in the direction of the flow."

Figure 64
Oscillographic Record of NERVA Ejector
Showing Shock-Pattern Breakdown

Figure 65
Exit Mach Number vs Chamber Pressure Ratio
(NERVA Ejector Model)
 $A_D/A^*=35$ Gimbal $\angle=0^\circ$

Reference

Reynolds, W. C., Consultant Report Evaluation of Proposed
Model Experiments for Test Cell "D", February 1961

Figure 66
Transitory Stall in a Boundary Layer
with Adverse Pressure Gradient

B. TEST METHODS

A series of tests were run in the three conditions listed in IX,A,above, to determine the direction of boundary-layer flow. Fiber tufts were mounted at the duct exit in the boundary layer in order to observe boundary-layer phenomena.

Visual observation of the fiber tufts was made for all three cases, and photographs were taken of the Condition (3) tests, as part of the Flow-Visulation Program.

Nozzle-chamber pressure was varied to provide the following conditions:

1. Shutdown: P_c range 100 to 0 psig
2. Shock pattern breakdown: (See Figure 64)

$$\frac{P_c}{P_a} > \left(\frac{P_c}{P_a} \right)_{\text{start}} \quad \text{to} \quad \frac{P_c}{P_a} < \left(\frac{P_c}{P_a} \right)_{\text{start}}$$

3. Supersonic flow at exit: (See Figure 65)

$$\frac{P_c}{P_a} > 48$$

C. TEST RESULTS

The results of all the back flow analysis tests indicate that there is no backflow into the duct at any of the three conditions tested.

Figure 67 shows a shadowgraph photo taken during a test of Condition 3. As apparent, the tufts are positioned to indicate positive duct flow.

Figure 67
Shadowgraph of Flow Pattern
at Duct Exit ($P_c=9000$ psia)

D. CONCLUSIONS AND RECOMMENDATIONS

The results of the backflow-analysis tests lead to the conclusion that there will always be positive flow in the boundary at the duct exit during NERVA engine operation.

A further refining test could be the introduction of a flammable gas into the scale-model exhaust system, and the subsequent ignition of this gas. This test would simulate the actual disposal of the nuclear-engine exhaust, indicating the length beyond the duct exit, so that sufficient mixing with the air would allow burning of the gaseous mixture. An analysis would have to be performed so that scaling factors could be determined and applied to the test results.

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SECTION XI

DESIGN IMPLICATIONS

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XI. DESIGN IMPLICATIONS

The results of the test program on the scale model ducts for the NERVA and Phoebus engines have been applied to the full-scale duct systems to check the suitability of the designs.

A. FLOW CONDITIONS

The gas-dynamics tests indicated that the duct configurations would operate satisfactorily, and provide altitude simulation for the engine during steady-state operation. The tests further indicated that both the "pull-in" and "drop out" of the cell pressure would be stable. However, indications of flow instability in the elbow region were detected at the NTS-design pressure ratio. The lack of knowledge concerning this type of ejector emphasizes the problems that unstable flow conditions could create in the full-scale ejector, and makes elimination of this condition desirable (although not a necessity since the engine compartment did not have this instability).

B. HEAT-TRANSFER RATES

The heat-transfer rates for the full-scale ducts will be quite high in the elbow region, and will invalidate the aluminum-tube design concept for this region. The heat flux of 12 Btu/in²-sec, as scaled from the model tests for the NERVA ejector, is of the same magnitude as the throat heat flux of many rocket engines, and would result in a short life expectancy for an alternate stainless steel elbow. It would appear that some method of reducing this heat flux will be required before an ejector design can be obtained which will meet the requirements for the Test Cell "D" facility. The desired reduction in heat flux could be obtained with the present ejector configuration by mass injection, although the film cooling tests made during this program were not encouraging. The test results indicate that the mass injection changes the flow dynamics and does not penetrate the high heat-transfer rate region in the elbow. This indicates that considerable experimental work would be required to apply this approach to the full-scale ejectors.

The qualitative agreement of the heat-transfer rates in the ejector with experience in open-flame deflectors indicates that the desired reduction can be attained in the easiest manner through changes in ejector configuration. These changes would produce more gradual turning of the exhaust gases, and should also benefit the gas dynamics through the nearer approach to a straight ejector configuration.

C. DESIGN OF SECONDARY EJECTOR FOR TEST CELL "D"

1. Selection of Parameters

The secondary ejector for Test Cell "D" was designed with the aid of the secondary ejector tests, and using a nozzle centerline angle, ϕ , of 12.5° , as a compromise between optimum performance and ease of fabrication. The nozzle wedge angle, λ , was chosen to be 15° , again a compromise, since a larger value of λ would cause flow separation, and a smaller value would produce nozzle-fabrication difficulties. A working fluid of superheated steam was chosen, based on the availability of water and cost considerations.

The value of (A_D/A_S^*) was chosen as 10.75; a larger value would allow a lower environmental cell pressure, while a smaller value would allow a lower secondary ejector chamber pressure.

The value of (A_{S2}/A_S^*) was determined to be 5.36; a larger value would allow a lower chamber pressure, and while smaller value would lead to fabrication problems.

2. Theoretical Performance and Application to Design

The theoretical starting pressure ratio is 8.6 (Figure 68). The secondary-ejector-nozzle chamber-pressure ratio was taken to be 30% higher than the theoretical value required for start. This was because

the theoretical analysis neglected frictional and other losses, fabrication tolerances may change the secondary ejector nozzle geometry, the secondary ejector operating chamber pressure should be sufficiently greater than the starting pressure to account for possible changes in atmospheric pressure, P_a .

The chamber temperature was determined from the criteria that the steam should not condense when expanded.

The secondary ejector nozzle design parameters are tabulated in Figure 69.

The generator selected to supply steam to the secondary ejector utilizes liquid oxygen and propane for combustion. This results in a gas of 86.2% superheated water vapor and 13.8% carbon dioxide being delivered to the secondary ejector.

Figure 68
 Theoretical Performance of
 Full-Scale Secondary Ejector (Steam + CO₂)
 Nozzle Angle = 12.5°

WORKING FLUID	γ	$\dot{W}$ (lb/sec)	P_{sc} (psia)	T_{sc} (°F)	A_D/A_s^*	A_{se}/A_s^*	ϕ	β	θ	λ
STEAM CO ₂	1.28	290.	143.	1350.	10.75	5.36	12.5°	5°	20°	15°

Figure 69
Secondary Ejector Nozzle Design
for Full-Scale NERVA Ejector

D. PURGING ANALYSIS

The purge tests showed that, if the nitrogen inlet temperature is the same as the ambient air temperature, then the amount of N_2 needed for full-scale application can be determined from Figure 70, if $\dot{w}_{N_2}$ is made constant. The value of V_2 for the NERVA ejector is 7460 ft^3 . When the nitrogen inlet temperature differs from the ambient air temperature, then equations (27) and (31) must be solved by taking incremental time elements.

To obtain an idea of the maximum amount of N_2 needed to purge the ejector, consider the following assumed case:

Let

$$\%O_2 = 1\% \text{ by volume (to be conservative)}$$

$$T_{N_2} = T_a = 540^\circ\text{R}$$

$$P_{2_{\text{cell}}} = 1.7 \text{ psia}$$

$$R = 55.2 \text{ ft}/^\circ\text{R}$$

then $w_{N_2} = 190 \text{ lb}$ from equations (35) and (36) or from Figure 70.

If P_{cell} is 1 psia, then $w_{N_2} = 110 \text{ lb}$.

Figure 70
Volume of Nitrogen Required to Reduce
the Percentage of Oxygen in the Test-Cell Atmosphere

E. BACKFLOW ANALYSIS

The test program for backflow indicated that there was no need for special design considerations to be provided in the construction of the full-scale ejector system for prevention of backflow at the ejector exit.