THERMAL BEHAVIOR AND OPERATING REQUIREMENTS OF IFE DIRECT-DRIVE TARGET

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ABSTRACT

During injection, inertial fusion energy (IFE) direct drive (DD) targets are subject to heating from energy exchange with the background gas and radiation from the wall. This heat deposition could lead to deuterium-tritium (DT) phase change and target deformation violating the target physics symmetry requirements. This paper assesses the thermal behavior of the target under such conditions and explores possible ways of extending the target lifetime.

I. INTRODUCTION

A typical IFE DD target consists of a spherical shell (~4 mm in diameter) composed mainly of solid DT at 18 K, as illustrated in Fig. 1. It is injected at velocities up to about 400 m/s in an IFE chamber. Once the target reaches the center of the chamber, the driver is fired, focusing energy on the target to create the fusion micro-explosion. To obtain the maximum energy yield from the fusion reaction, the temperature of the frozen DT layer must be held at about 18.5 K and the target must maintain a high degree of spherical symmetry and surface smoothness.

During injection, the target is subject to heating from energy exchange with the background gas as well as from radiation from the chamber walls. The resulting target thermal behavior is a particular concern because the target can deform due to melting and evaporation of the DT. Although the maximum temperature limit to prevent unacceptable target outer layer deformation is not well known, the previous assumption was to maintain the target DT temperature (T_{DT}) below the triple point ($T_{TP,DT} = 19.79$ K) [1].

This paper presents an updated and more detailed assessment of the target thermal behavior during injection. The heating threats are first characterized. The initial thermal analyses based on maintaining $T_{DT} < T_{TP,DT}$ are then summarized. Next, possible ways of increasing the target design operating windows are investigated in a two-prong fashion: (1) enhancing the target thermal robustness through design modifications; and (2) exploring the possibility of extending the DT thermal limit by better understanding the effect of energy deposition and phase change on the target density symmetry relative to target physics requirements. For this purpose, a simple semi-integrated thermo-mechanical model has been derived to provide supporting analyses. Finally, conclusions are provided including recommended directions for future R&D effort.
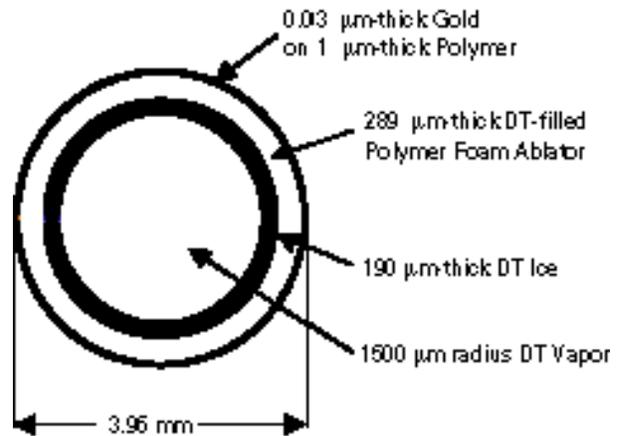

Figure 1   Example direct-drive target configuration [1]

II. TARGET HEATING

An important component of the heat transfer to the target is associated with the transfer of energy from the impinging background gas (such as xenon or helium) which might be needed in the chamber for wall protection and through which the target must travel. Such heating processes would include "convection" as well as condensation if the background gas boiling and melting points are higher than the target temperature (e.g. for Xe). In addition, if plasma conditions remain in the chamber, recombination of ions at the surface would result in even larger heat transfer. Much uncertainty remains regarding plasma conditions during injection which is currently being investigated. For simplicity, plasma effects are not included in the calculations presented here with the understanding that the results would have to be revisited as new information on plasma remnants in the chamber becomes available.

Convection heat transfer from the background gas to the target has been analyzed previously over different regimes (molecular, transition and continuum) but without explicitly considering condensation [2]. For a 4-mm target, the transition regime (Knudsen number ~0.1-10) applies for a Xe pressure, $P_{Xe} \le$ ~100 mtorr (at 300 K) and the full molecular regime for $P_{Xe} \le$ ~1 mtorr (at 300 K). These include the range of pressures anticipated for a DD target with a dry wall; clearly continuum regime convection heat transfer would not apply. For this reason, a direct simulation Monte Carlo (DSMC) program was used to determine the incident heat flux, $q_{inc}^{"}$, at the surface of an IFE target [3]. The DSMC computations of $q_{inc}^{"}$ assume that the temperature of the impinging Xe atoms drops to 18 K, but that no Xe atoms stick to the target surface. Rather, they drop in temperature and then reflect from the surface where they provide a shield against subsequent atom collisions. However, depending on the condensation coefficient, a significant fraction of Xe atoms could stick to the target surface thereby not shielding subsequent atom collisions and resulting in higher, more challenging heat fluxes on the target. A condensation analysis was done to assess this possibility [3].

Figure 2 shows the maximum condensation heat flux, $q_{cond}^{"}$, as a function of the product of P_{Xe} and condensation coefficient, σ_c for cases with different Xe temperatures, T_{Xe}, and injection velocities. These results were found to be consistent with those from DSMC runs for σ_c's ranging from ~0.5 at 100 mtorr to ~1 at 1 mtorr. No data were found for the condensation coefficient for Xe at ~1,000's K condensing on an 18 K. However, experimental data from Ref. [4] indicate σ_c values of 0.99-0.6 for 2,500 K Ar beam condensing on an 15 K Cu/Ar with incident angle of 0°-60°. Thus, it seems prudent to assume σ_c values of ~1 to estimate $q_{cond}^{"}$.

Results for $q_{inc}^{"}$ from a He background gas were found to be somewhat higher than those from the Xe case even in the absence of condensation. This can be explained as follows: (1) the latent heats have only a small effect on the overall energy transfer which is mostly governed by the change in the gas enthalpy; and (2) the molecular fluxes of He on the moving target are higher than those of Xe for the same pressure and temperature [3]. However, use of He has the advantage that the He atoms after transferring their energy to the 18 K target will likely be reflected back thereby shielding the target from subsequent He atom collisions.
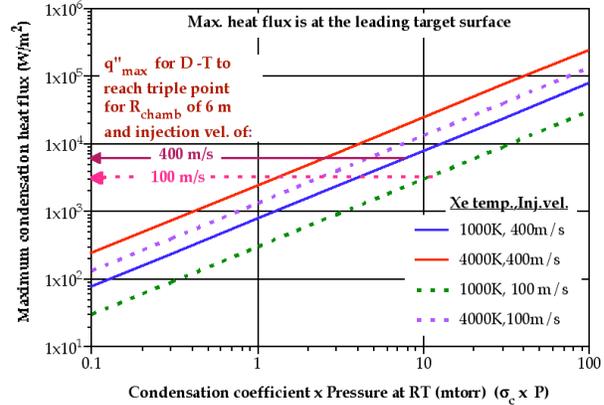

Figure 2   Maximum $q_{cond}^{"}$ as a function of the product of σ_c P_{Xe}(at 300K) for cases with different T_{Xe},'s and injection velocities.

A simple estimate of the radiation heat flux, $q_{rad}^{"}$ on the target during injection is given by:

$$q_{rad}^{"} = (1-\rho)\sigma_{S-B}T_w^4 \qquad (1)$$

where T_w is the wall temperature (assumed as a black body), σ_{S-B} is Stefan-Boltzmann constant, and ρ the target surface reflectivity. A very reflective target surface is required to minimize the total absorbed heat flux. For the very thin (275–375 Å) coating of gold on the target, a reflectivity of ~96% is anticipated[1]. An effort is underway to estimate more accurately the radiated energy absorption and reflection based on a multi-layer wave model to provide a stronger basis for this assumed reflectivity. As an illustration, $q_{rad}^{"}$ from eq. (1) ranges from 2300 W/m^2 for T_w=1000K to 11,000 W/m^2 for T_w=1500 K.

III. TARGET THERMAL ANALYSIS

The thermal response of the target (shown in Fig. 1) to $q_{inc}^{"}$ was determined parametrically assuming a 2-D heat flux distribution over the target similar to those from the DSMC results[3]. The ANSYS finite element code[5] was used for this transient thermal analysis which assumed that the target is not tumbling (i.e. the same side of the target is always facing forward and the leading edge of the target is exposed to the maximum $q_{inc}^{"}$ during the entire time of flight). Temperature dependent DT properties were used including the latent heat of fusion $T_{TP,DT}$ to model the phase change [3].

Figure 3 summarizes the results for a target injected at 400 m/s in the chamber. It shows the maximum DT temperature change as a function of the maximum heat flux at the target surface for 3 different

chamber radii (affecting the time of flight for a given injection velocity). The temperature change of the target increases in a linear fashion up to the triple point of DT. At the triple point, there is a knee in the curve where additional heat flux does not affect the temperature as much, consistent with the phase change when going from solid to liquid DT. From the figure, the heat flux to reach the triple point is about 6000 W/m^2 for a 6-m radius chamber and even lower for larger chambers. From Fig. 2, this corresponds to a q''_{cond} from Xe at 1000K and 7.6 mtorr or at 4000 K and 2.5 mtorr (for σ_c =1); and from eq. (1) to q''_{rad} from a wall at 1275 K. This would place an important constraint on background gas density that might be required for wall protection.
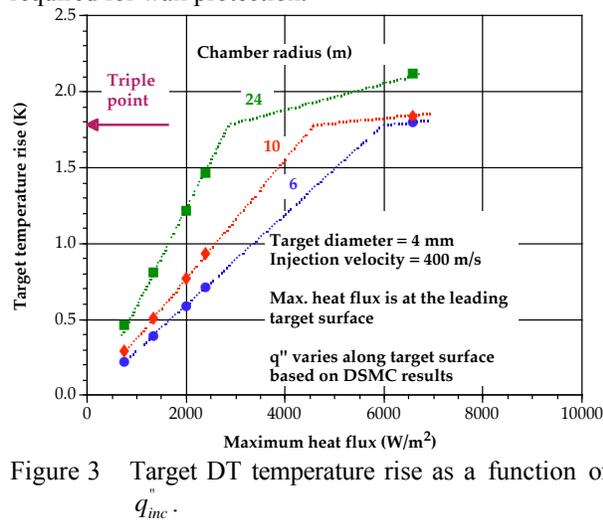

Figure 3   Target DT temperature rise as a function of q''_{inc}.

IV. TARGET DESIGN MODIFICATION TO ENHANCE THERMAL ROBUSTNESS

One of the measures envisaged to enhance the thermal robustness of the target is the inclusion of an porous plastic foam layer (of thickness δ_{ins} and fractional density ρ_{ins}) on the outside of the target to provide thermal insulation that would delay the heat transfer to the DT region of the target and help extend its lifetime during injection. One assumption is to replace some of the DT-foam region by this outer porous foam layer subject to confirmation from target physics requirements [6].

The temperature properties of the cryogenic foam were based on those of fully dense polystyrene. The density was adjusted according to ρ_{ins}. For simplicity, the thermal conductivity of the porous foam was similarly adjusted and then further scaled by a factor of 2/3 to account for possible optimization of the porous micro-structure to minimize the conductivity. As a

conservative measure, the higher thermal conductivity values found in the literature were used in this study consistent with Ref. [2], with values ranging from 0.088 W/m-K at 19 K to 0.13 W/m-K at 40 K. The heat capacity values used range from 100 J/kg-K at 20 K to 225 J/kg-K at 40 K[6].

The transient analyses were performed using ANSYS and the results are illustrated in Figure 4 for a maximum q''_{inc} of 2.2 W/cm^2 (e.g. corresponding to condensation of 10 mtorr, 4000 K Xe from Fig. 2). The figure shows the DT interface temperature history during injection for a target with a 25% dense outer foam layer of various thicknesses. In this case, a δ_{ins}~130 μm (32 μm of equivalent solid polystyrene) would be sufficient to maintain $T_{DT}<T_{TP,DT}$ at the interface for a flight time of 0.015 s (corresponding to a target velocity of 400 m/s in a chamber of radius 6 m). As comparison, $T_{DT}=T_{TP,DT}$ after about 0.0022 s in the absence of the outer foam layer.
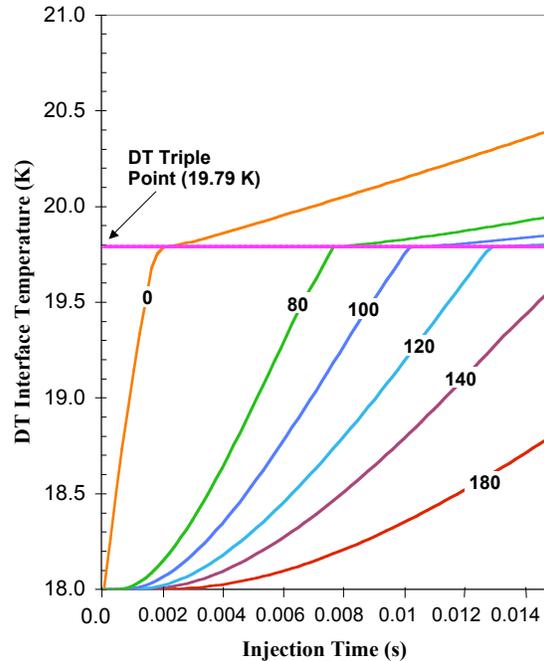

Figure 4   DT interface temperature history for various thicknesses (shown in microns on the lines) of a 25% dense outer foam insulating layer under an incident heat flux of 2.2 W/cm^2.

The results also showed that the time for DT to reach the triple point is increasingly retarded as ρ_{ins} is decreased due in good part to the large increase in heat capacity as the temperature of the foam increases. For

example, for a 100 μm outer foam region the time for DT to reach its TP increases from ~0.106 s to ~0.126 s as ρ_{ins} is reduced from 25% to 10%. Thus, for increased target thermal robustness, it is preferable to increase δ_{ins} and reduce ρ_{ins} of the outer foam layer to values that can still accommodate the target physics and structural integrity requirements. For example, a 152 μm-thick 10% dense insulating foam layer would accommodate a heat flux of up to 7.5 W/cm^2 (e.g. corresponding to condensation from 1000K, 100mTorr Xe) while maintaining DT below its TP after up to ~0.015 s of flight time.

The possibility of increasing the plastic coating thickness, $t_{plastic}$, from ~1 μm to 10 μm to provide added insulation was also assessed but was found to provide only marginal improvement.

V. THERMO-MECHANICAL ANALYSIS

Another means to extend the target lifetime is by relaxing the DT thermal limit through a better understanding of the effect of energy deposition and phase change on the target density symmetry relative to target physics requirements. From Fig. 1, the target is covered by a ~1 μm solid plastic coating. Under heating, the concern is that vapor might form at the DT-foam/plastic coating interface resulting in unacceptable density variation for proper target functioning. If the bond between DT-foam and the plastic coating is perfect, vapor formation will only occur through homogeneous nucleation. However, homogeneous nucleation was estimated as being virtually zero at temperatures lower than 26 K while dramatically increasing as the temperature approaches 34 K [7]. Heterogeneous nucleation could be a problem but assumes the presence of nucleation sites of the order of 1 μm. This pre-supposes some imperfection in the DT-foam/plastic coating bond. Such imperfection could also lead to a micro-gap at the interface which would favor surface evaporation. This scenario was further assessed.

It was assumed that as heat reaches the DT-foam/plastic coating interface during injection, the DT temperature increases to $T_{TP,DT}$ and DT starts to melt and to vaporize. The pressure build-up due to phase change (both DT liquid and vapor have lower densities than solid DT) must be counterbalanced by the stress in the plastic coating which provides the structural element. A simple semi-integrated thermo-mechanical model was derived for the analysis and is described in Ref. [7]. The phase change thickness and DT interface temperature were first estimated as a function of $q_{inc}^"$

from a series of 2-D ANSYS runs and used as input for the thermo-mechanical model. The model solves for the pressure of the DT liquid and vapor corresponding to the given parameters and consistent with the DT pressure and temperature relation at saturation (obtained from its phase diagram).

Results from the analysis are summarized in Figures 5-7. Figure 5 shows the vapor region thickness as a function of the maximum $q_{inc}^"$ for different $t_{plastic}$'s. The vapor region thickness is lower for thicker plastic coating. For an 8-μm thick coating, the evaporated layer thickness is <2.5 μm for $q_{inc}^"$ <4 W/cm^2.
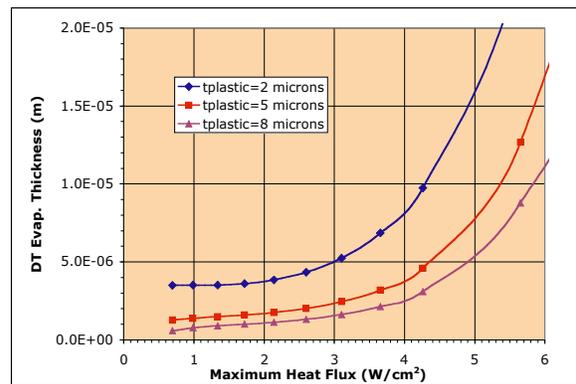

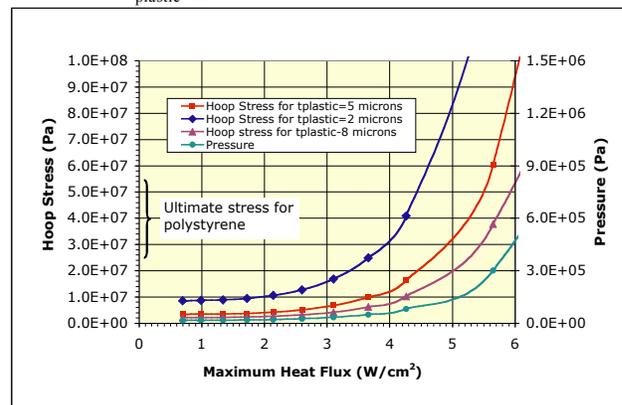Figure 5   Vapor region thickness as a function of the maximum incident heat flux for different $t_{plastic}$'s.

Figure 6   Hoop stress in the plastic coating as a function of $q_{inc}^"$ for different $t_{plastic}$'s. Also shown is the equivalent pressure in the DT liquid and vapor regions.

Figure 6 shows the hoop stress in the plastic coating as a function of the heat flux for different $t_{plastic}$'s. Also shown is the equivalent pressure in the DT liquid and vapor regions. The ultimate tensile strength of polystyrene is ~30-60 MPa. Based on the lower value, the maximum allowable heat flux ranges from 4 to 5.5 W/cm^2 for $t_{plastic}$'s of 2 to 8 μm,

respectively. Figure 7 shows the average vapor temperature as a function of the heat flux for the different $t_{plastic}$'s. Also shown is the vapor/liquid interface temperature.

Allowing for vapor formation would relieve the demand on design modifications such as adding an insulating foam layer for reasonable heat flux accommodation. Example results for a case with a 72 μm, 25% dense outer foam layer indicate that the allowable incident flux to maintain an evaporation layer thickness ~3 μm is ~9 W/cm² for $t_{plastic}$ = 8 μm. This thickness corresponds to about a 1% density variation for the 289 μm DT-foam region. The corresponding hoop stress is < 30 MPa.
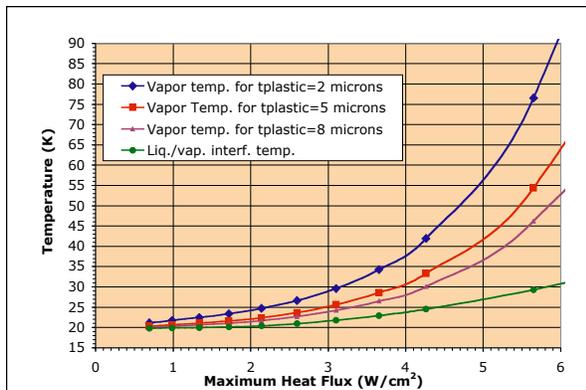

Figure 7   Average vapor temperature as a function of $q_{inc}^{"}$ for different $t_{plastic}$'s. Also shown is the vapor/liquid interface temperature.

The results from this simple thermo-mechanical model have helped to highlight the benefits of relaxing the DT vapor formation constraint and of including design modifications such as an insulating outer layer. However, this model has limitations. It does not include the latent heat of vaporization which can affect the vapor conditions. It also assumed a rigid DT ice inner boundary. Any deformation of this boundary would relieve the pressure and affect vapor formation. It also can be cumbersome relying on curve fitting of ANSYS results to provide the necessary relations between heat flux, interface temperature and phase change thickness. A fully integrated model including the interactions of all key processes would be a very useful tool to further understand the phase change process and its effect on the target symmetry. Such an integrated model would include a better phase change models, integration of thermal and mechanical processes and would calculate the transient thermo-mechanical and phase change behavior in a consistent way.

VI. CONCLUSIONS

The potential benefit on target lifetime of adding an insulating foam layer and/or of better understanding vapor formation processes have been examined. For the typical target configuration shown in Fig. 1, the maximum $q_{inc}^{"}$ for DT to reach its triple point is only about 0.6 W/cm² for a 6-m radius chamber and even lower for larger chambers. This would place an important constraint on background gas density that might be required for wall protection. Adding an insulating outer foam layer on the target helps to increase the target lifetime. Adding a 130 μm 25% dense outer foam layer would increase the allowable $q_{inc}^{"}$ for DT to reach its triple point to 2.2 W/cm² and a 152 μm 10% dense insulating foam layer would accommodate a heat flux of up to 7.5 W/cm². For increased target thermal robustness, it is preferable to have the maximum thickness and porosity outer foam layer which can still accommodate the target physics and structural integrity requirements.

Allowing for vapor formation would relieve the demand on design modifications such as adding an insulating foam layer for reasonable heat flux accommodation. A simple thermo-mechanical model was developed to help in better understanding the DT phase change process. A thicker plastic coating was found preferable to reduce the vapor region thickness. If it were assumed that ~1% change in region density was acceptable based on target physics requirements, ~ 3 μm of vapor region at the DT-foam/plastic coating interface would be acceptable. Under these conditions, the maximum allowable $q_{inc}^{"}$ is ~4 W/cm² for the original target design and up to 9 W/cm² for a target design with 72 μm thick, 25% dense outer insulating foam layer and an 8 μm thick plastic coating. In both cases, the corresponding hoop stresses in the plastic coating are less than the anticipated ultimate tensile strength.

The results from the simple thermo-mechanical model have helped to highlight the benefits of relaxing the DT vapor formation constraint and of including design modifications such as an insulating outer layer. However, this model has limitations and a better understanding of the phase change processes would be obtained from a fully integrated model including the interactions of all key processes. For example, the assumption of surface evaporation is conservative and infers the presence of a minute gap at the DT-foam and plastic coating interface; for a good-quality interface bond, DT boiling is more likely to occur through nucleation which should be included in the model. This

also indicates the need for an experimental effort to better characterize the quality and behavior of this bond ideally by using or possibly by simulating the actual materials. In addition, guidance is needed from the target physics perspective to understand better the constraints and limitations imposed on such actions.

NOMENCLATURE

P_{Xe} Xe pressure;
q''_{cond} condensation heat flux on target;
q''_{inc} incident heat flux on target;
q''_{rad} Sradiation heat flux on target;
$t_{plastic}$ plastic coating thickness;
T_{DT} DT temperature;
$T_{TP,DT}$ DT triple point =19.79 K;
T_w wall temperature;
T_{Xe} Xe temperature;
δ_{ins} porous plastic foam layer thickness;
ρ target surface reflectivity;
ρ_{ins} porous plastic foam layer fractional density;
σ_c condensation coefficient;
σ_{S-B} Stefan-Boltzmann constant.

ACKNOWLEDGEMENT

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